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# THE ENGINEERS' DIGEST

REVIEW OF ENGINEERING PROGRESS ABROAD

VOLUME V

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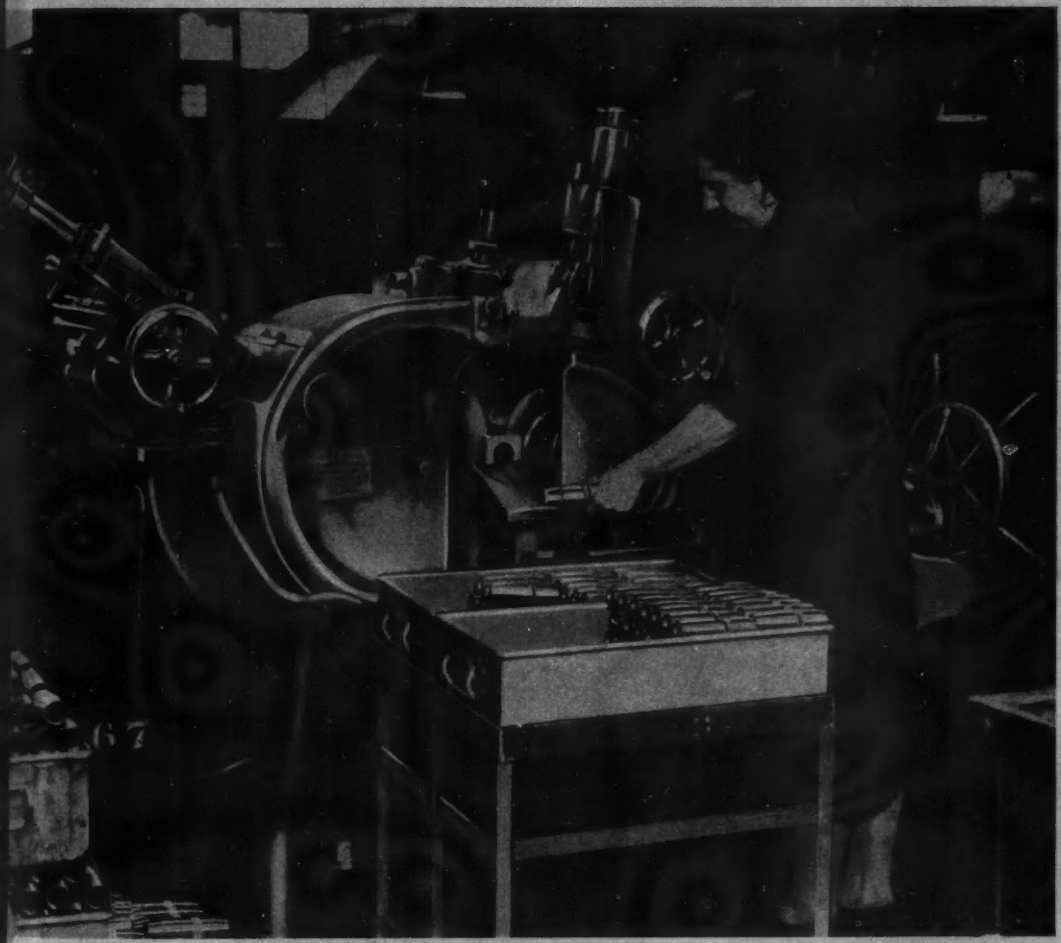

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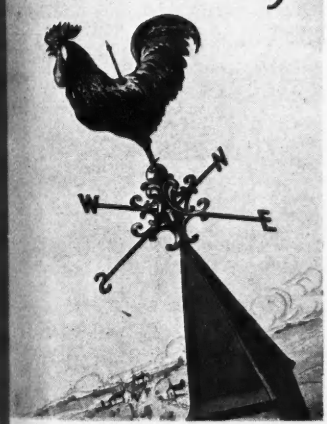

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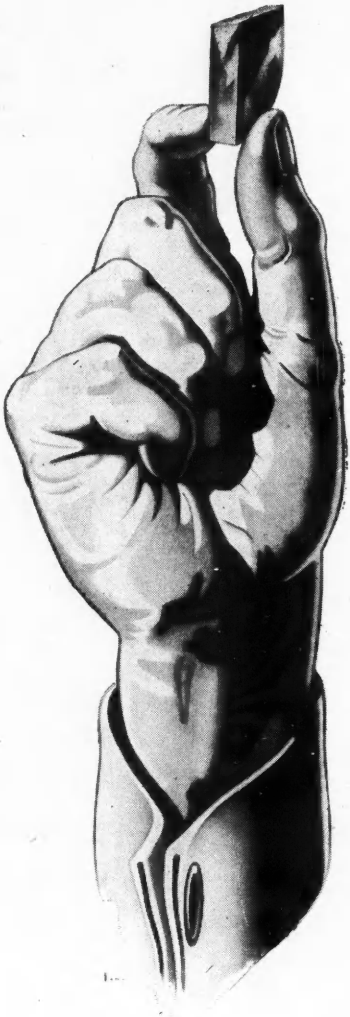

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# THE MEASUREMENT OF POWER ON AERO ENGINES IN FLIGHT.

By W. DIEPENBRUCK, Berlin. (From *Luftwissen*, Vol. 10, No. 6, June, 1943, pp. 165-170.)

## Measurement of Engine Speed.

When filight testing new aeroplane types, the engine power under all flight conditions must be known to get a clear answer as to aerodynamic performance.

The imitation of actual operation conditions meets unsurpassable difficulties, especially with high altitude aero-engines. With reference to the present stage of development, a test plant fully conforming to actual conditions would have to operate at a wind speed up to 230 m/sec., a temperature of 56.5 deg. C, barometric pressure of 0.16575 kg/cm² (at a height of 12 km, I.C.A.N. standard atmosphere) and a humidity up to 100%. The cost of the equipment and the operational cost of such a plant would, of course, be extremely high. On the other hand, power measurement in flight offers the advantages of inexpensiveness, simplicity, and saving of time and labour.

Although aeroplane and airscrew designers obtain data on the engine power from the engine makers, they are interested to measure the power themselves, as this is always more reliable than its estimation by means of coefficients (height, temperature, humidity, etc.). The performance data are also more readily obtained when simultaneously measuring the engine power.

The highly developed art of dynamometry as practised in modern test plants, does not, however, lend itself to adaptation as aircraft equipment because of the principle of its working. The difficulties are partly due to the crowded arrangement and operational conditions prevalent on aircraft.

## Measurement of Torque.

The power of the engine is generally determined from the torque on the shaft and from the engine speed.

Instruments measuring the engine speed can be based on three various principles: 1. Counting the number of revolutions of the crankshaft in a given period. 2. Measuring the equivalent of angular velocity such as centrifugal force, electromagnetic force of an eddy current system or the voltage of a direct or alternating current generator. 3. Using a standard of known number of revolutions, such as a vibration tachometer or the stroboscopic method.

The mechanical centrifugal type of engine speed indicator is now less and less in use, on account of the limited distance from the engine of the flexible shaft. This type can be used on small single engine aircraft. In general use are the electrical types (direct and alternating current), the accuracy of which can still be increased, although a limit is set by the temperature influence. Practically error-free results can be obtained by the revolution counter of the mechanical or electrical type.

## Requirements on Torquemeters.

Torque applied to a machine part—in this case transmitted by the engine crankshaft to the airscrew—is dimensionally the product of a force and a length, and cannot, therefore, be directly measured. What can be measured are its effects, such as force, deformation, or by transformation, electrical or thermal energy.

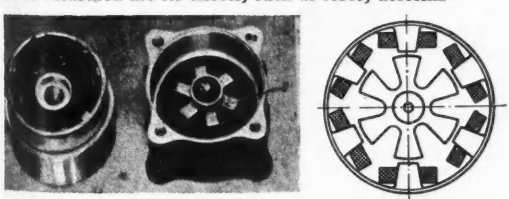


*Fig.* 1. Siemens & Halske a.c. generator without commutator. 1 = Armature winding; 2 = Rotating magnet wheel.

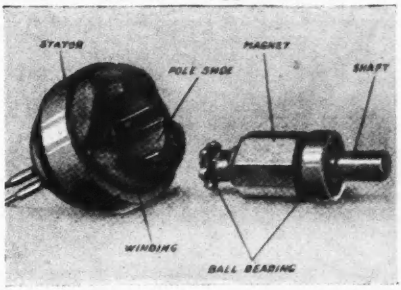



*Fig.* 2. Parts of Dr. Th. Horn's a.c. generator.

In practice, the torque in the unit engine-airscrew offers possibilities to measurement by the following effects:—

(*a*) Torsion of the line shaft between engine and airscrew.
(*b*) Peripheral forces on the toothed wheels of the reduction gear or at other discontinuities in the line shaft.
(*c*) Tangential forces due to torque reaction on the fixed gear rim of planet reduction gear (with radial engines).
(*d*) The torque reaction of the whole engine in the form of a change in bearing load as compared with the idling condition.

Under the action of torque, all members between crankshaft and airscrew are subject to elastic deformation. These members represent the spring connecting the two-mass system crankshaft-engine. To measure the deformation in the sense here applied, three possibilities are open:

(*a*) If the deformation is of a magnitude enabling the measuring instrument to be applied in "parallel" arrangement, the spring constant c*m* of the measuring instrument must be either zero or negligible as compared with that of the system to be investigated.
(*b*) If the deformation is insufficient to allow accurate measurement, the "springs" represented by the measuring instrument must be attached in "series." Thereby the natural frequency of the total system is considerably lowered. Any possible danger to the engine

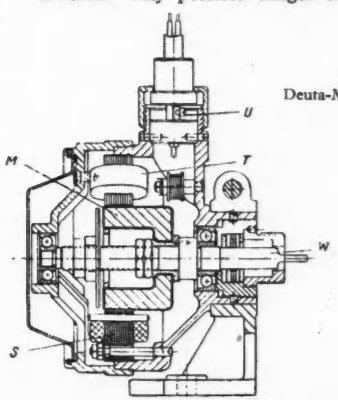



*Fig.* 3
Deuta-Morell a.c. generator.

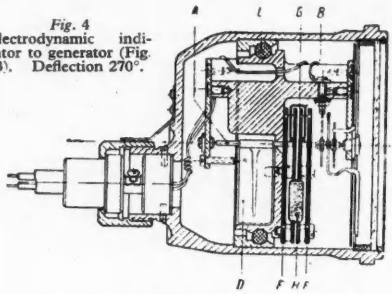


*Fig.* 4
Electrodynamic indicator to generator (Fig. 3). Deflection 270°.

plant must be carefully considered in each individual case.

(*c*) When, for technical reasons or reasons of space (accessibility) the two previous methods break down, use is made of a hub dynamometer, the torsional stiffness of which is so high as to leave the torsional vibration of the engine uninfluenced. The magnitude of the deformation to be measured is then of the order of the arrangement described under (*a*).

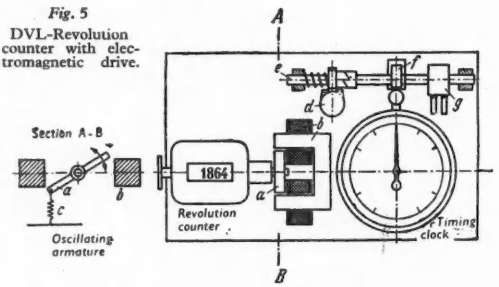



*Fig.* 5
DVL-Revolution counter with electromagnetic drive.

a = Oscillating armature; b = Electromagnets; c = Spring
d = Switch button; e and g = Switching shafts; f = Triangular cam

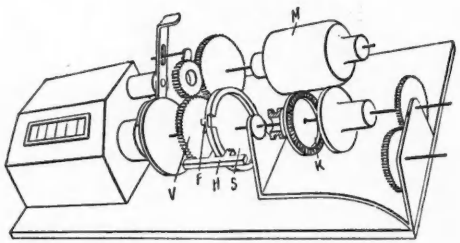



*Fig.* 6. Schematic arrangement of the FKFS Revolution counter.
M = Synchron motor; V = Back gear; S = Switch wheel; F = Notch; H = Switch lever; K = Clutch.

## REQUIREMENTS OF TORQUEMETERS.

### Magnitude of Torque.

The power of present-day aircraft reaches 2000 h.p. per engine. The corresponding engine speed measured on the crankshaft is 2000–3000 r.p.m., which is reduced via gear to 1400 r.p.m. The resulting torque on the crankshaft is about 480–720 kgm, on the airscrew shaft round 1000 kgm. Superimposed on these is alternating torque due to uneven combustion and resonance of ca. 100–800 Hz. Thus, the greatest torque on the airscrew shaft is 3000 kgm, that on the crankshaft up to 2160 kgm.

### Sensitivity.

For routine testing, the knowledge of engine speed and mean torque over a certain period is of interest. When tests are made on newly developed engine types, knowledge of the torque distribution on the crankshaft over the period of four strokes (2 revolutions) is required as a basis for investigations on vibration of the engine-airscrew system and for fatigue studies on parts subject to alternating loads.

These two purposes define also the sensitivity of the instruments required. For measuring the mean torque, the sluggishness of a pointer indicator is just sufficient as the torque is constant over a period of 2–3 seconds, the period of measurement proper.

The measurement of torque distribution, however, requires a recording instrument of much greater sensitivity.

### Accuracy.

Instruments used for torque measurement in flight cannot have the great accuracy obtained in stationary testing. The accuracy reached in flight tests, however, is of the order of 2% of the maximum value measured. An exception is the DVL hub dynamometer with an accuracy of ± 4 kgm within its range (ca. 0.3% at 1500 kgm). The accuracy of the instruments should be the same at all heights, i.e., at all temperatures and pressures. Where the accuracy is dependent on these, exact correction factors must be ascertained.

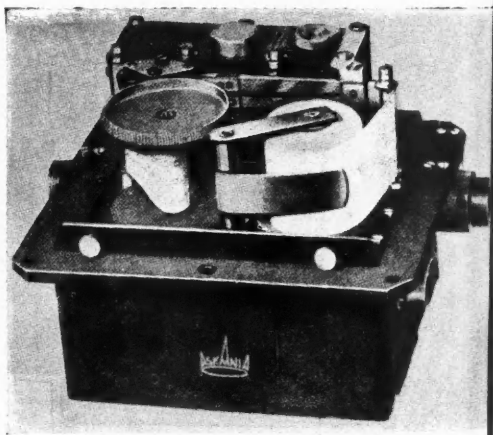


*Fig.* 7. Askania Revolution counter with lid unscrewed.

# NOTES ON THE HEAT TREATMENT OF GRAY CAST IRON.

By M. BADER. (From "*Schweizer Archiv für angewandte Wissenschaft und Technik*," Vol. 9, No. 5, May, 1943, pp. 148-156).

OWING to the fact that the ground-mass of cast iron possesses a steel-like character, cast iron can be heat-treated like steel. The most commonly applied methods of heat treatment are (1) stress-relief anneal, (2) soft anneal, (3) hardening and tempering, (4) salt-bath hardening, and (5) surface hardening with the oxy-acetylene torch.

1. *Stress relief of cast iron.*

While almost complete stress relief can be effected by annealing at temperatures up to 500 deg. C., a far reaching internal stress reduction can be obtained with temperatures of 250-300 deg. C. if the annealing time is correspondingly prolonged. Any creation of new internal stresses must be avoided by slow and uniform cooling subsequent to the annealing process. Generally, stress relief anneal is carried out at temperatures excluding structural changes of the ground-mass, particular care being taken to prevent the initiation of graphitisation, as this is the only way of keeping loss in strength to a minimum.

2. *Soft anneal of gray cast iron.*

Soft anneal is applied for the purpose of either eliminating hard spots in complicated, thin walled casting, or of increasing permissible cutting speed in machinery. For this purpose, decomposition of the pearlitic groundmass is deliberately aimed at. Recent investigations have shown that normal commercial gray cast iron can be satisfactorily graphitised, that is, soft-annealed, by heat treating at a temperature immediately below that of the pearlite point, hereby reducing both oxidation and distortion. Also, any free cementite which may be present, as well as the cementite of the pearlite, will decompose more or less into ferrite and graphite according to the actual temperature prevailing, almost complete decomposition being attained at 700-730 deg. C. If annealing temperatures and time are properly chosen, even castings with thinnest possible wall thickness and with widely different carbon and silicon content can be softened in this way.

3. *Hardening and tempering of gray cast iron.*

By cooling normal gray cast iron slowly from temperatures above the pearlitic point, the austenite is transformed into pearlite. But as austenitic non-magnetic cast iron does not possess a pearlite change-point, this material cannot be hardened and tempered. In the same way as with steel, control of the quenching speed of cast iron permits of the production of structures varying from austenite to coarse lamellar pearlite. For the sake of obtaining a satisfactory structure, the temperature of quench should lie well above the pearlite interval; but actual quenching temperature must be determined by test. With tempering temperatures ranging from 200-250 deg. C., the original tensile strength of the material is recovered; while maximum possible tensile strength is obtained with temperatures of draw from 350-450 deg. C. As generally the increase in strength obtainable with tempering is relatively small, hardening and tempering is carried out chiefly for the sake of producing a wear-resistant martensitic structure. With cast iron, this is obtained if transformation takes place at 200-250 deg. C. In order to diminish the danger of crack formation, attempts have been made to depress the change point by the addition of nickel.

4. *Salt-bath hardening of gray cast iron.*

Wear-resistant structure can also be obtained by either the metal or salt bath method, both of which, in the case of complicated casting, avoid thermal stresses and danger of cracking. This is due to the fact that with sufficiently long immersion in a bath of properly chosen temperature, the martensitic stage is altogether avoided. The pieces are kept in the bath until the austenite is completely transformed; after that, the piece is cooled to room temperature.

5. *Surface hardening with the oxy-acetylene torch.*

Surface hardening can be carried out with the use of the oxy-acetylene torch, a very high flame temperature being required to effect rapid heating of the surface of the piece, and with it concentration of the heat in the surface. The kind of austenite-pearlite structure obtained will depend upon temperature and speed of the quench.

*Experiments.*

A number of tests were carried out in the gray cast iron foundry and in the testing department of the EISEN & STAHLWERKE VORM. GEORG FISCHER at SCHAFFHAUSEN, SWITZERLAND, with two cast iron materials of the following analysis:

| | Total C % | Fixed C % | Graphite % | Si % | C + Si % | Original Bending Strength kg/mm² | Original Tensile Strength kg/mm² |
|---|---|---|---|---|---|---|---|
| Ge No. 1 | 2.86 | 0.81 | 2.05 | 1.46 | 4.32 | 57.5 | 38.1 |
| Ge No. 2 | 3.36 | 0.85 | 2.51 | 2.38 | 5.74 | 41.5 | 23.9 |

Test specimens for tensile testing were subjected to the following treatment:

1. Stress relief: Anneal at 600 deg. C. for 20 hours, cooling-off in the furnace.
2. Graphitisation: Anneal at 730 deg. C. for 20 hr., cooling-off in the furnace.
3. Hardening: Oil-quench from 840 deg. C.
4. Heat treatment: Oil quench from 840 deg. C., tempering for 6 hr. at 250 deg. C., cooling in air.
5. Heat treatment: Oil quench from 840 deg. C., tempering for 6 hr. at 350 deg. C., cooling in air.
6. Salt-bath hardening: Quenching from 840 deg. C. in salt bath of 250 deg. C. with 15 min. immersion, cooling in air.
7. Salt-bath hardening: Quenching from 840 deg. C. in salt bath of 350 deg. C., with 15 min. immersion, cooling in air.

The tensile strength obtained with each of these seven different treatments are charted in Fig. 1. Referring to the properties exhibited by cast iron No. 1, with which the quenching temperature lies above the pearlite interval, its increase in hardness is seen to be accompanied by a decrease in tensile strength. Vice versa, when tempered, its increase in tensile strength coincides with a fall in hardness. At the tempering temperature of 350 deg. C., its tensile strength is greater than with tempering at 250 deg. C.

A. Original condition
B. Hardened from 840 deg. C.
C. Oil quenched from 840 deg. C., tempered at 250 deg. C., and cooled in air for six hours
D Oil quenched from 840 deg. C., tempered at 350 deg. C., and cooled in air for six hours
E. Quenched from 840 deg. C. in 250 deg. C. salt bath for 15 minutes, and cooled in air
F. Quenched from 840 deg. C. in 350 deg. C. salt bath for 15 minutes, and cooled in air
G. Stress-relief by annealin for 20 hours at 600 deg. C. cooled in furnace
H. Graphitisation by 20 hour anneal at 730 deg. C. cooled in furnace.

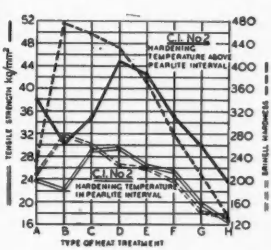

TENSILE STRENGTH kg/mm²
BRINELL HARDNESS
C.I. No 2
HARDENING TEMPERATURE ABOVE PEARLITE INTERVAL
C.I. No 2
HARDENING TEMPERATURE IN PEARLITE INTERVAL
TYPE OF HEAT TREATMENT
A B C D E F G H

Fig. 1. **Tensile strength and Brinell-hardness of heat-treated cast iron.**

Fig. 2a. Ge 1. With 2.92 per cent carbon and 1.46 per cent silicon. In original condition. Pearlite fine. Magnification 500x.

Fig. 2b. Ge 2. With 3.36 per cent carbon and 2.38 per cent silicon. In original condition. Pearlite somewhat coarse. Magnification 500x.

The reverse results in salt-bath hardening, where the test pieces hardened in a bath of 250 deg. C. showed greater tensile strength than those hardened at 350 deg. C.

According to Figs. 2a and 2b, cast iron No. 1 in the original state shows a finer pearlite ground-mass than No. 2 with a higher silicon content.

It has been mentioned before that stress-relief anneal must be carried out at a temperature low enough to preclude commencement of graphitisation. The structures produced with annealing for 20 hours at the extraordinarily high temperature of 600 deg. C. are shown in Figs. 3a and 3b. Here it is seen that the softer cast iron No. 2 (with a higher C and Si content than No. 1) is in an advanced state of graphitisation as compared to No. 1. It follows that cast iron with high carbon and silicon percentages must not be annealed at a temperature exceeding 500 deg. C.

As shown in Figs. 4a and 4b, the graphitisation aimed at in soft annealing is obtained with a temperature of 700-730 deg. C., which is below the pearlite interval, the actual choice of temperature and duration of the soft anneal being governed by the carbon and silicon content of the piece. In order to demonstrate the influence of the hardening temperature, a hardening temperature of 840 deg. C. was chosen because this lies above the pearlite interval of cast iron No. 1 and within that of No. 2. Under this condition, cast iron shows a 100 per cent martensitic structure (Fig. 5a); while No. 2 contains almost exclusively ferrite, plus coagulated martensite (Fig. 5b). A martensitic structure of cast iron No. 2 can, however, be obtained with a quench temperature of 900 deg. C., which lies above the pearlite interval of this material.

Fig. 3a. Ge 1. Stress relief by anneal for 20 hours at 600 deg. C. Pearlite. Magnification 500x.

Fig. 3b. Ge 2. Stress relief by anneal for 20 hours at 600 deg. C. Ferrite plus residual pearlite. Magnification 500x.

Fig. 4a. Ge 1. Graphitisation by anneal at 730 deg. C. for 20 hours. Ferrite and Graphite. Magnification 500x.

Fig. 4b. Ge 2. Graphitisation by anneal at 730 deg. C. for 20 hours. Ferrite and Graphite. Magnification 500x.

According to the temperature and duration of tempering, the martensitic structure is transformed into a softer structure as e.g., troostite or sorbite. Cast iron No. 1 hardened in a salt bath of 250 deg. C. acquires the troostite-structure shown in Fig. 6a; while cast iron No. 2 possesses a structure of troostite plus ferrite, as evidenced by Fig. 6b.

A number of salt bath tests at 250, 250, 450, and 550 deg. C. bath temperature were carried out to establish the S-curve of a cast iron Ge No. 3 of 2.98 per cent carbon and 1.85 per cent silicon. The original structure of this material is shown in Fig. 7a. According to the S-curve, the austenite will have its greatest

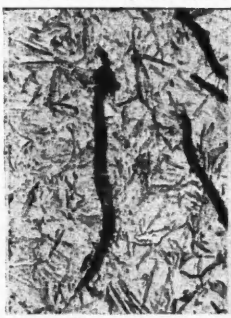


Fig. 5a. Ge 1. Oil quench from 840 deg. C. Hardening temperature above the pearlite interval. Martensite. Magnification 500x.

Fig. 5b. Ge 2. Oil quench from 840 deg. C. Hardening temperature within pearlite interval. Ferrite plus coagulated martensite. Magnification 500x.

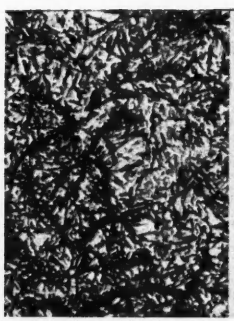


Fig. 6a. Ge 1. Hardened by quench from 840 deg. C. in salt bath at 250 deg. C. for 15 minutes, cooled in air. Troostite. Magnification 500x.

Fig. 6b. Ge 2. Hardened by quench from 840 deg. C. in salt bath at 250 deg. C. for 15 minutes, cooled in air. Ferrite plus Troostite. Magnification 500x.

stability if the test piece at hardening temperature is immersed as quickly as possible in a salt bath of 250 deg. C. In fact, during an immersion time of two minutes in a bath of 250 deg. C. incipient austenite transformation was hardly noticeable, the piece exhibiting structureless martensite after cooling, Fig. 7b. But after 14 minutes immersion at 250 deg. C., almost complete transformation of the austenite could be recorded. To judge from Fig. 7c, the structure of this piece corresponds to a martensite with a small amount of acicular troostite.

A piece immersed for two minutes in a salt bath at 550 deg. C. exhibited a partial transformation of the austenite, while immersion for 14 minutes resulted in complete transformation of the latter. With a bath temperature of 450 deg. C., 50 per cent of the austenite had been transformed after two minutes, the microphotograph showing about 50 per cent structureless martensite and about 50 per cent acicular troostite. After 14 minutes immersion austenite transformation was complete, the structure showing a fine acicular-troostite. Finally, with a bath temperature of 550 deg. C., austenite transformation was complete after two minutes immersion.

Fig. 7a. **Ge 3. With 2.98 per cent carbon and 1.85 per cent silicon. In original condition. Pearlite. Brinell hardness 269. Magnification 500x.**

Fig. 7b. **Ge 3. Hardened by quench from 870 deg. C. in salt bath of 250 deg. C. for two minutes, cooled in water Structureless martensite. Brinell hardness 444. Magnification 500x.**

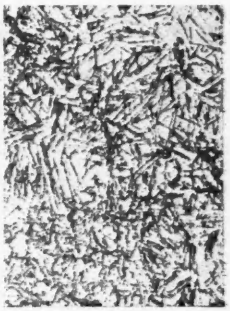


Fig. 7c. **Ge 3. Hardened by quench from 870 deg. C. in salt bath of 350 deg. C. for 14 minutes. Cooled in water. Brinell hardness 415. Magnification 500x.**

# THE SULPHUR CONTENT OF CARBURIZING COMPOUNDS IN ELECTRIC CARBURIZING FURNACE OPERATION.

By K. WOHLGEMUTH. (From *Fertigungstechnik*, No. 4, July, 1943, pp. 92-94.)

WITH the introduction of electrically heated carburizing furnaces into hardening practice, attention had to be given to the effects of the sulphur contents of the carburizing compounds upon the electrical heating elements. The latter, usually made of high chromium nickel alloys, show great resistance to the oxygen, water vapour, carbon dioxide and carbon monoxide constituents of the furnace atmosphere, notwithstanding the high temperature of 850–930 °C. employed in carburizing. But this is not so, however, in the case of sulphur dioxide and sulphuretted hydrogen, the latter actually having most harmful effects upon any chromium and nickel alloyed high temperature materials. Setting in at about 700° C., this destructive action greatly increases in intensity with rising temperatures.

This instability is generally attributed to the high nickel content of these materials, the nickel reacting at high temperatures with sulphur or $H_2S$ to form an easily melting sulphide. It is this sulphide which is responsible for damaging the mechanical, electrical and thermal properties of the chrome-nickel material to such a degree that it eventually becomes completely useless. Sulphur-dioxide in itself would be much less aggressive than the sulphuretted hydrogen, were it not for its conversion into sulphuretted hydrogen in a reducing furnace atmosphere. These difficulties are not limited to the resistor elements, but also occur with the carburizing boxes, the pyrometer sheaths and other parts. Incrustation of ash, fly-ash, and loam and clay containing masses used for sealing the boxes, are also dangerous in this respect, as they may become enriched with sulphur, thus transmitting the harmful action of the latter to the parts concerned by direct contact.

For these reasons, it is being increasingly demanded that in view of the experiments made, carburizing compounds for use with electrically heated furnaces should contain no more than 0.5%–1.0% sulphur. As the carburizing compounds in use to-day consist of the coked products of mineral coal, brown coal, and peat, etc., it is natural that they contain considerable percentages of the sulphur compounds present in the raw materials. In the main, these sulphur compounds are: (1) pyrite ($FeS_2$); (2) metallic sulphides, especially FeS; (3) metallic sulphates; and (4) organically combined sulphur. During gasification and coking, these sulphur compounds are subjected to conversion, the pyrite being almost completely transformed into FeS and $H_2S$, while the metallic sulphates are reduced to corresponding sulphides. Finally, volatile organic sulphur compounds are also formed. The total sum of the sulphur compounds contained in the coked products is generally termed "Total sulphur," and can be taken as a characteristic factor to be used, for instance, in determining the suitability of a carburizing compound for use in electrically heated furnaces. But this procedure may easily lead to an exaggeration of the danger, and cause the rejection of carburizing compounds the total sulphur content of which may not be more harmful than that of low-sulphur carburizing compounds. A standard definition of maximum permissible total sulphur content does not therefore yet exist, nor would it be an easy matter to establish it. The aforementioned limiting values of 0.5%–1.0% must thus be considered as entirely arbitrary factors of safety to which unquestionable reliability cannot be ascribed.

In recognition of the true situation, present day trends are to judge the suitability of carburizing compounds for use in electrically heated furnaces on the basis of "volatile sulphur" contents rather than on that of "Total sulphur." Lack of unification in the definition of this term calls here for a clarifying statement. This is that a basic difference exists between what is termed "volatile" sulphur fraction in coking or gasification process and that named "combustible" sulphur in processes of actual combustion. This distinction is perhaps best formulated thus:

Volatile sulphur = Total sulphur minus sulphur in coke.

Combustible sulphur = Total sulphur minus sulphur in ash.

Concerning carburizing compounds, the definition of "volatile sulphur" is further narrowed down by the stipulation that it is taken to refer to the sulphur escaping during the entire period of the carburizing process and at the carburizing temperature employed.

As no way has been found as yet to establish a relationship between the total sulphur content and the volatile sulphur content, the latter can only be determined experimentally. Unfortunately, even this experimental determination still meets with the difficulty that

no methods exist which can be considered as absolutely reliable, and which can be quickly carried out. In view of this, the introduction of this new concept of carburizing compound characteristics will be bound up with the development of new methods for the determination of the volatile sulphur contents.

Since present-day commercial carburizing compounds are mixtures of materials with greatly different sulphur percentages, greatest care must be taken in taking samples for the determination of the sulphur percentage. Still greater difficulties exist in the accurate analytical determination of the volatile sulphur, the percentage of which lies within the limits of error of the analytical method, as can be seen from the subjoined table.

| Carburizing Compound | Total S after burning | Volatile S (*a*) | (*b*) |
|---|---|---|---|
| I | 0.65% | 0.07% | 0.07% |
| II | 1.22% | 0.02% | 0.04% |
| III | 0.32% | 0.03% | 0.03% |
| IV | 2.61% | 0.12% | 0.1 % |

(*a*) By precipitation with barium chloride.
(*b*) By difference: total sulphur minus sulphur in ash.

Of these four compounds, No. I is low in ash, mild-acting, and suitable for electrical furnaces. The compound No. II has a more drastic effect, and is not suitable for electrical furnaces; compound No. III, also having a drastic action, is low in sulphur and suitable for electrically heated furnaces; while, owing to its high sulphur content, compound No. IV is unsuitable for electrically heated furnaces.

The analytical determination of the "volatile sulphur" is carried out by heating one gram of the substance for one hour at 900° C. with oxygen passing slowly over it, and the products of combustion being passed through one % solution of hydrogen peroxide. The sulphuric acid thus formed is then quantitatively determined by precipitation with barium chloride. A comparison of results obtained by this precipitation method with corresponding data obtained by difference (total sulphur minus sulphur in ash) yielded fully satisfactory agreement between the values obtained. But there still remains one minor inaccuracy insofar as the new method leads to the determination of the combustible sulphur content which exceeds the volatile sulphur content by a certain, although very small, amount.

# ELECTRIC STEAM RAISING FOR LOCOMOTIVES.

By W. MULLER and E. MEYER, Bern. (From *Bulletin Schweizerischer Elektrotechnischer Verein*, Vol. 34, No. 8, 21st April, 1943, pp. 210-214.)

DESPITE the need to reduce coal imports, the Swiss Federal Railways have been unable to convert the whole of their system to electric traction owing to shortage of materials. Although only 5 per cent. of the total tonkms. is now handled by steam locomotives, a good deal of shunting work is still done by steam, as it has not been possible to replace the original steam locomotives by suitable electric units, nor, for similar material reasons, has it been possible to complete the full electrification of all yards and sidings.

The S.F.R. thus still possess about 100 steam-shunting engines of the 0-6-0 type built before 1915, and it was decided to convert two of them experimentally to electrical steam raising so that their boilers could be operated alternatively or simultaneously by means of coal or electricity. The present article gives a description of two engines converted by Brown, Boveri & Co., Baden. The cost of conversion amounted to about 100,000 Swiss francs per locomotive, this sum having to be set against the annual saving of about 300 tons of coal.

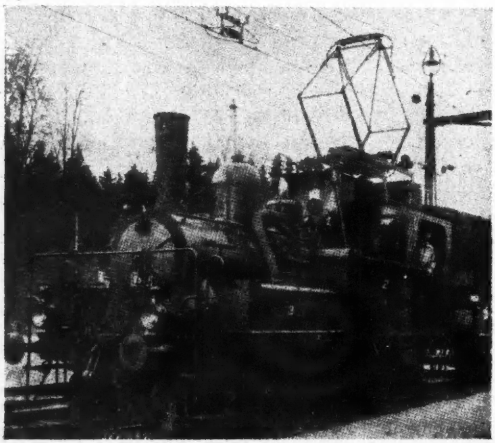


**Fig. 1. Shunting locomotive of the S.F.R. equipped with electrical steam raising equipment.**
1. Main switch. 2. Transformer. 3. Evaporator.

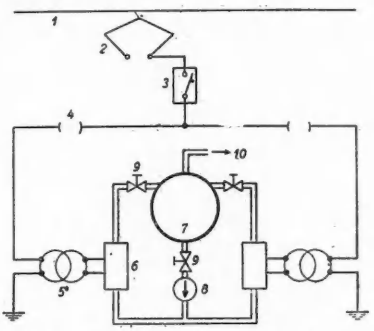



**Fig. 2. Diagrammatic layout of the electrical steam raising equipment.**
1. Overhead conductor line
2. Pantograph
3. Main switch
4. Isolating switch
5. Transformer
6. Evaporator
7. Main boiler
8. Circulating pump
9. Valve
10. Main steam pipe

The weight of the locomotives (without the electrical equipment) is 35 tons. The boilers have a capacity of 2.2 m³, 56.5 m² heating surface, and are built for 12 kg/cm² steam pressure. The locomotives have two cylinders 360 mm. dia. with a stroke of 500 mm.

The electrical equipment consists of two evaporators, one electrically driven pump for water circulation, and of the necessary equipment for collecting the 16⅔ c/s, 15,000 V. single-phase current and for stepping down the voltage.

There are two similar evaporators, one on each side of the locomotive (Fig. 1). Each consists of a nest of 16 steel tubes parallel to the axis of the locomotive, each 2.3 m. long and of 37.5 mm. inner and 44.5 mm. outer diameter. Through them circulates the water pumped from the boiler of the locomotive, and at the same time they carry the current which heats them, and thus evaporates the water (Figs. 2 and 3). The tubes are thus heater elements and must, therefore, be electrically insulated from the evaporator casing and from the locomotive as a whole. This requirement and the low electrical resistance of the water tubes are the reasons for the low voltage (20V) used in the heating system. The capacity of each evaporator being about 250 kW,

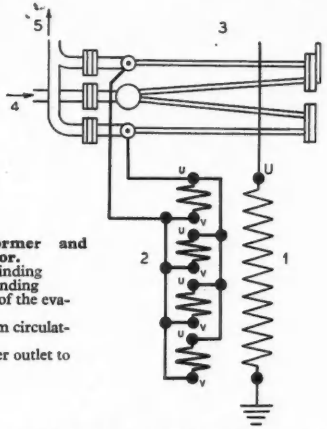



**Fig. 3. Transformer and evaporator.**
1. High voltage winding
2. Low voltage winding
3. Heating tubes of the evaporator
4. Water inlet from circulating pump
5. Steam and water outlet to boiler

the heating current is approximately 12,000 A. The water flows through each evaporator at the rate of 5 litres per second. Usually evaporation is only partial, a mixture of steam and water being returned to the boiler (Fig. 2).

The heating tubes are thermally and electrically insulated by layers of very fine glass-wool, and the whole nest is enclosed in a box of sheet iron. The steam mains leading outward are insulated by asbestos. The connections of the heating tubes with the rest of steam and water circuit, and the supports and clamps holding the tubes, must be such as to ensure adequate electrical insulation. Asbestos is used in this connection.

There are two transformers, each close behind its respective evaporator, to which it is electrically connected by a number of copper leads. The transformers are of the usual oil immersed design, their ratio is 15,000/20 V, and they are rated for 240 kVA on continuous load. Their primary windings are connected to two isolating switches, thence to one common oil circuit breaker, which in turn is connected to the pantograph on the roof of the driver's cabin. There is no switchgear or other apparatus in the secondary circuits of the transformers where the current may reach as much as 16,000 A.

The evaporators possess no automatic regulation; when the production of steam exceeds the consumption the current must be switched off by hand. It can be switched on only if all valves are open and if the evaporators are filled with water, for which there is automatic control. Electricity supply to the transformers is automatically cut off when the temperature of the evaporator tubes rises above a certain limit; due to failure in the water supply; when the primary current becomes too high; when the current supply in the overhead conductor is interrupted, or when the pantograph is lowered.

The motor of the feed-water circulating pump, the coil of the main switch, and the lighting in the driver's cabin, are supplied from a battery of 36V and of 100 Ah capacity. This battery is charged through a rectifier

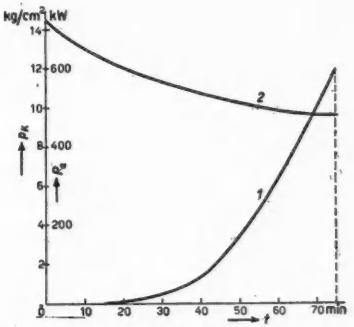



**Fig. 4. Starting-up period with electric evaporation.**
1. Steam pressure $P_k$ as function of the time t.
2. Electrical input $P_a$ as function of the time t.

connected to the secondary winding of one of the transformers and installed on the roof of the cabin.

The total weight of the electric steam raising equipment is 7 tons, of which 4.8 tons are accounted for by the transformers, and 1.2 tons by the evaporators. The total weight of the locomotive was thus increased from 35 tons to 42 tons, which required the addition of one plate to each bearing spring.

No other alterations were necessary, as the original boiler was completely retained and remained unchanged. The driving controls and the injector feed remained as before; speed, tractive effort, etc., being regulated from the steam side.

Also, the facilities for coal firing were retained, thus allowing the engine to be operated either on coal or on electricity, or on both simultaneously. The latter is of particular importance in cases where only part of the sidings have overhead conductors, or when the service is exceptionally heavy and additional coal firing is required. This may occur as the electric equipment, for economic reasons, is laid out for medium service only and is inadequate for occasional peaks, in which case the engine may be operated on "mixed evaporation." The coal firing being auxiliary, the engine can be operated throughout by one man.

Tests have shown that the hourly consumption of coal averaged 85 kg., and that the corresponding electricity consumption was about 480 kWh.

The time taken to raise steam from cold is 55 to 75 minutes for engines with electrical heating, against 2 to 3 hours if coal fired. The starting-up time is, of course, a function of the outside temperature, of the water temperature and of the water level in the boiler. Fig. 4 shows the steam pressure and the electrical input during the starting-up period. The electrical input decreases because the electrical resistance of the tubes increases with the temperature. The energy consumed for starting up is approximately 600 kWh. In service the heat losses amount to 75 to 90 kW.

The results obtained with the described locomotives fulfilled all expectations, and it is believed that further engines will be fitted with similar evaporators as soon as the economic circumstances will permit.

# WEIGHT OF STEEL LATTICE POLES FOR LONG DISTANCE LINES.

By DIPL. ING. V. PÁNTYIK, Györ, Hungary. (From *Elektrotechnika**, Budapest, Vol. 36, No. 7, July, 1943, pp. 192-97).

A SUFFICIENTLY accurate knowledge of the weight of poles is very important in the preliminary stages of planning and design.

Their price is calculated on a unit-rate based on weight and a considerable percentage of the total cost of long-distance lines is due to the poles, e.g., for a 22 kV long distance line the steel poles comprise 20-30% of the total cost.

It would at first appear desirable to increase the distance between poles to reduce their number. With wider spacing, however, the sagging of the cable increases, which necessitates higher poles. This could be avoided by using cables of higher tensile strength so that the height of the poles could be kept constant, but

* Official organ of the Hungarian Electrotechnical Association, Budapest.

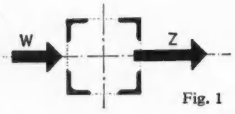


Fig. 1

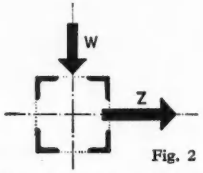


Fig. 2

the greater tension on the poles makes it necessary to strengthen them structurally, which again means greater weight. It can be seen that for finding the most economic spacing of poles an accurate assessment of their weight is essential.

To obtain the weight data given in this paper, the statistics of about one hundred poles were analysed. The assumptions throughout were that the cross-section of the poles is quadrilateral and, that the cable tension Z and wind pressure W are parallel and in the same direction (Fig. 1). The latter assumption gives greatest weight as the four corner angles and the lattice bars of the four sides of the poles (which are equally dimensioned) must be calculated for the load Z + W.

This is evident when considering a second loading case (Fig. 2), where Z is perpendicular to W. The four corner angles are again dimensioned for Z + W; the lattice bars, however, are calculated for the load Z or W, according to which is the greater.

The weight determination has been carried out for two classes of poles, namely:

1. Poles with a single foundation; the lattice bars —after developing the pole sides into one plane—are nearly parallel. For brevity these poles are classed as "low poles" (Fig. 3).

2. Poles with four separate foundations; the lattice bars of the four sides of the pole are of identical design. Designated as "high poles" (Fig. 4).

**The Weight of Low Poles.**

If the pull of the cables, taken at the tip of the pole is Zm (in kg.) and the total height of the pole from the ground is H(m), the weight of the pole can be read directly from diagram shown in Fig. 5. For completeness, the figure shows also the type of poles to which the weight data refer. The dimensions are given in mm.

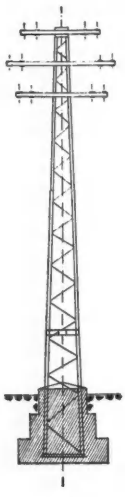


Fig. 3

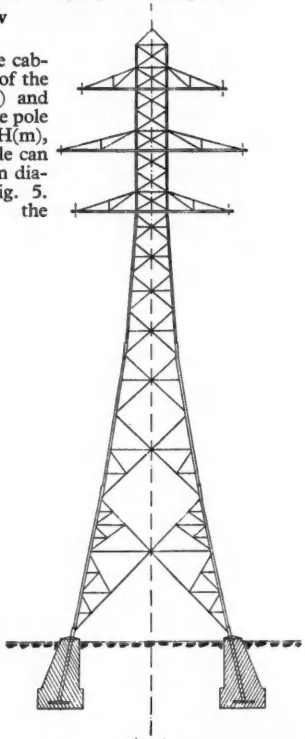


Fig. 4

**Example.**

The weight of the corner pole shown in Fig. 6 is to be determined when the three duralumin cables have a cross-sectional area of 70 mm² each, the tensile strength of the cables is $\sigma = 11$ kg/mm², and the angle they include is $\alpha \times 110°$. The dimensions of the pole are given in Fig. 6.

The pull at the top of the pole is

$$Z = \frac{70 \times 11}{12.50} (10.7 + 11.2 + 12.4) = 2120 \text{ kg.}$$

The resultant pull on the pole is then

$Zm = R = 2\ (2120 \times \cos\ 55°) = 4240 \times 0.574 = 2430$ kg.

The wind pressure (W) need not be brought into the calculation separately as it is taken care of in the weight estimate on which the diagram is based. Interpolating

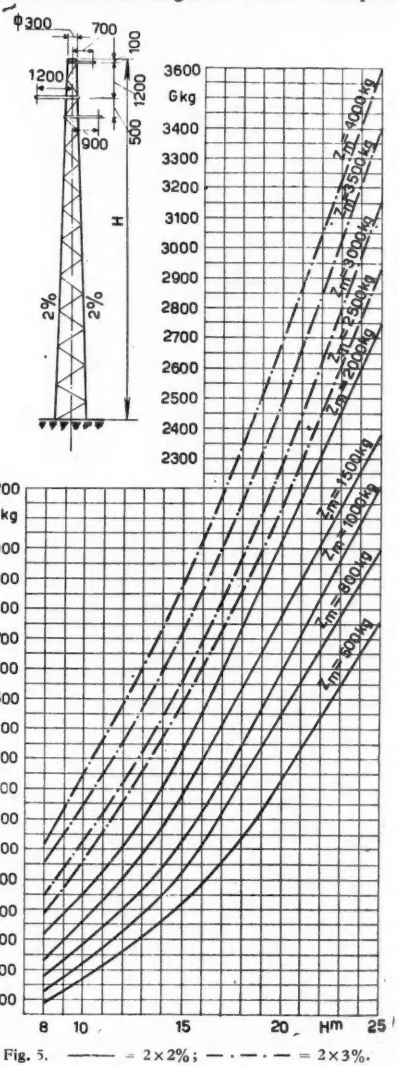


Fig. 5. ——— = 2×2%; — · — · — = 2×3%.

in Fig. 5 for Zm = 2430 kg, at H = 12.5 m. we read the weight of pole, G = 1160 kg. From the diagram we find that this pole should be designed with a taper of 3%; the curve in question being chain-dotted. The weight thus found in extreme cases is ± 5% of the actual weight of existing poles.

**The Weight of High Poles.**

The weight for high poles is obtained in the same way as for low poles (Fig. 7), and the previous remarks as

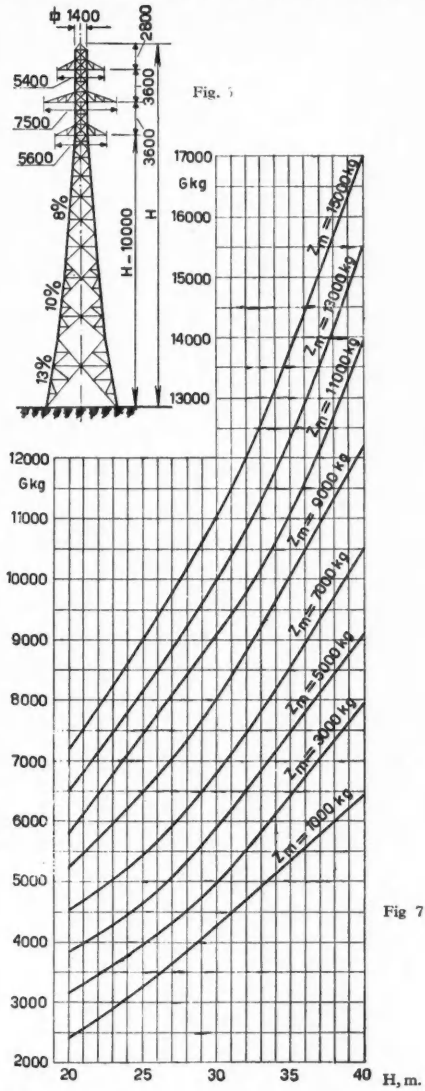



Fig. 5

Fig 7

to details and use of the diagrams apply here as well. The tapering of the high poles is 13% for the bottom section, 10% for the middle section, and 8% for the top section respectively, and is the same for all values of Zm (Fig. 7). The weight data (Fig. 7) are exclusive of various additional devices such as ladders, steps, railings, insulators, etc., as these are not standardized. The inset figure of the pole in Fig. 7 is typical for poles for which the weight graph is valid. The weight of high poles varies greatly with design, e.g., different latticing of the sides results in great variations in weight. For this reason it was not possible to compare the values given in Fig. 7 with a great number of poles actually built.

**Example.**

Given: H = 37.0 m, Cables of 6 × 95 mm² $\sigma$ = 26 kg/mm². The earth cable on the top of the pole is 1 × 50 mm² steel, $\sigma$ = 26 kg/mm². For further dimensions of the 10 m high, upper part of the pole see Fig. 7.

The one-sided pull on the pole is

$$Z = \frac{35 \times 26}{37.0} 2\,(27.0 + 30.6 + 34.2) + \frac{50 \times 26}{37.0} \times \times 37.0 = 13{,}500 \text{ kg.}$$

According to Hungarian standards AD21 (MOSz 151) the design value is

$$Zm = \frac{2}{3} Z = \frac{2}{3} \times 13500 = 9000 \text{ kg.}$$

For Zm = 9000 kg, and H = 37.0 m we obtain from Fig. 7 a weight G = 11000 kg. This figure again takes into account wind pressure.

## HEAT CONDUCTIVITY OF MIXED GASES

By Ing. M. Salamon.

(From *Elektrotechnicky Obzor*, Prague, Vol. 31, No. 12, December, 1942, p. 188).

Analysers for mixtures of two gases, for example as used for flue gas analysis, are generally based on the measurement of the heat conductivity of the mixtures. Usually the heat is measured by the absorption factor of a platinum spiral inserted in the path of the flue gases. Though the relation between the heat conductivity and the percentage composition of the mixture is found experimentally, it will be both convenient and useful to have a corresponding mathematical formula also.

As the thermal conductivity resembles electrical conductivity to a great extent, investigations were carried out regarding the possibility of applying the formula derived for the electrical conductivity of gas mixtures, as well as to their heat conductivity. The known formula for the electric conductivity $\kappa$ of a mixture of a number of materials with individual conductivities $\kappa_1, \kappa_2, \ldots \kappa_n$ is

$$\kappa^k = \Sigma\, v_1 \kappa_1^k,$$

where $v_1$ is the proportion of any individual component in the mixture ($\Sigma v_1 = 1$) and k a factor of the value $-1 < k < 1$. The actual value of k depends on the nature and proportion of the components. With k = O as an average value the above formula is transformed into:

$$\log \kappa = \Sigma\, v_1 . \log \kappa_1.$$

The problem is how to choose the factor k if this formula is to be applied to the heat conductivity of gases. For this purpose a series of measurements were carried out with various gas mixtures comprising two components and the factor k was found to be = 0.4. The formula for the heat conductivity of mixtures of two gases is therefore

$$\lambda^{0.4} = \Sigma\, v_1\, \lambda_1^{0.4}$$

Experiments with mixtures of hydrogen and oxygen showed a very close relationship between the measured and the calculated values.

# MAGNETIC METHODS FOR TESTING IRON.

By RUDOLF LESSOW, VDI, Berlin. (From *Maschinenbau/ Der Betrieb*, Vol. 21, No. 10, October, 1942, pp. 435-437.)

MAGNETIC methods as applied for testing iron and steel may also be used to a certain extent for testing non-ferrous metals. These methods make use of electrical measurements on test specimens or finished parts and make it possible to draw the following conclusions regarding :—

(i) Composition of the tested material ;

(ii) Previous treatment, e.g., forging, hardening, annealing ;

(iii) Dimensions

As all three factors influence the measurements it is necessary to keep two of them sufficiently constant to make definite conclusions possible regarding the variation of the third. This principle governs the use of magnetic methods for routine tests. The main fields of application are measuring the depth of case-hardened layers and testing the metallographic structure.

For evaluating hardness and the durability of tools, Rockwell, Vickers and similar tests are not always sufficient as the structure value of the steel is not sufficiently assessed. On small tools, such as twist drills, taps, etc., it is frequently impossible to carry out Rockwell tests.

## CHARACTERISTICS OF THE METHOD.

Nearly all instruments for making magnetic tests measure, either directly or indirectly, the iron losses during magnetising with a.c. According to Richter these losses are :—

Hysteresis $$N_H = \frac{\epsilon f}{100}\left(\frac{B}{10000}\right)^2 G \text{ Watt}$$

Eddy-currents $$N_W = \sigma\left(\frac{f}{100}\cdot\frac{B}{10000}\right)^2 G \text{ Watt}$$

In the formulae, $\epsilon$ = material constant
$f$ = frequency
$\sigma$ = co-efficient, depending upon the thickness of the tested specimen
$B$ = maximum induction
$G$ = weight of iron

The formulae show that there are two ways possible for testing :

(a) By measuring the various iron losses at constant frequency at constant induction, the material constant and the permeability influence the result.

(b) At variable frequency where the iron losses of the test pieces are equalized by varying the frequency. These variations of frequency are used to assess the qualities of the material.

Both methods have been used for practical purposes. For reasons of economy the constant frequency method has become of greater importance and for sensitivity of measurement a 50 ∞ a.c. system is sufficient.

The tests are arranged as comparison tests in which the specimen is compared with a standard having properties which are exactly known. Thus the magnetic characteristics are not directly measured in absolute values. The test instrument is calibrated with two standards, representing the upper and lower limits respectively. A piece having magnetic charateristics outside these limits is regarded as defective.

## TESTING INSTRUMENTS.

The instruments used are similar to those normally provided in the laboratory. There are three basic forms of circuit connection :

(1) For measuring by the voltage drop in the feeler coil (Fig. 1)

(2) For measuring through the secondary voltage of a transformer (Fig. 2)

(3) For measuring by the bridge current of an a.c. bridge. This current is caused to flow by the variation of the impedance of the feeler coil (Fig. 3)

In measuring the feeler coil voltage (Fig. 4), the feeler is a coreless coil, into which the test piece is inserted. The instrument A, measures the difference between coil voltage with the standard and with the piece tested. Any variations in the voltage of W, must be eliminated by electrical devices.

With the circuit arrangement (Fig. 5) the variations of W, are of less importance. The voltage of the coil encircling test piece P, is compared with the voltage of the coil encircling the standard N. The instrument A, may be a sensitive galvanometer. Scheme 6 is similar to that shown in Fig. 5, but the opposing voltage is derived from the source of current W. Either a coil with an iron core or a transformer according to Fig. 2 may be used as a feeler as well.

In the transformer method (Fig. 7) a transformer is used as a feeler, whilst the opposing voltage is derived from 50 cycle a.c. mains.

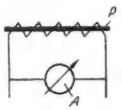



Fig. 1

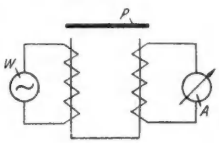



Fig. 2

**Basic elements for magnetic iron testers.**

*A* Indicating instrument
*W* Source of voltage
*P* Test piece

Fig. 1. **Measuring by coil voltage drop**

Fig. 2. **Measuring through the secondary voltage of a transformer.**

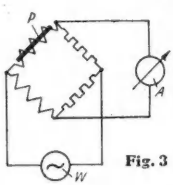
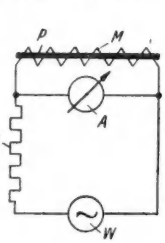
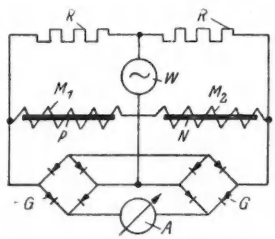
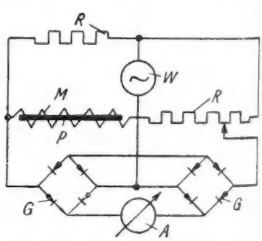



Fig. 3

Fig. 3. **Measuring by bridge current in an a.c. bridge**




**Magnetic testers measuring the feeler voltage**

*A* Indicating instrument, *G* Rectifier, *M* Feeler, *N* Standard, *P* Test piece, *R* Resistance, *W* a.c. source.

**Fig. 4.** Simple circuit for measuring the coil voltage.

**Fig. 5.** Differential connection with test piece and standard.

**Fig. 6.** Differential connection with opposing voltage derived from a.c. source.

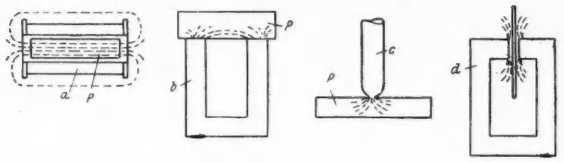



**Fig. 8. Lines of force in the test piece under different methods.** *a* **Coreless coil,** *b* **U-shaped iron yoke,** *c* **Straight feeler,** *d* **Feeler for light alloy sheets,** *p* **Test piece.**

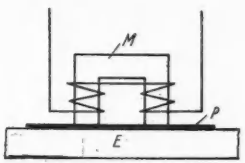



**Fig. 9. Measuring the thickness of non-magnetic layers.** *E* **Iron base,** *M* **Feeler,** *P* **Test piece.**

The bridge method shown in Fig. 3, variation of the feeler coil impedence causes a current to flow across the bridge and its magnitude is an indication of the qualities of the specimen tested.

## FEELERS.

Coreless coils (Fig. 8) may be used as feelers, or coils with a U-shaped iron core. The most appropriate shape depends on the form of the tested piece and on the qualities to be ascertained. For complicated forms of test pieces, or for those with wide variations in diameter, the coreless coil (a) is used if the structure of the material is to be probed (e.g., durability of cutting edge, hardness of through-hardened steel, etc.). If the depth of a case-hardened layer is to be tested it is better to pass the lines of induction through the hardened layer only and to use the U-shaped yoke (b).

The sensitivity of the instruments has been increased so much that a coil (c), Fig. 8, with a special straight iron core can be used in a holder similar to the usual stand for a precision gauge. The testing is done almost point by point.

The type (d) feeler is used for non-ferrous metals, e.g., light alloy sheets.

When using coils with iron cores, precision depends on the contact between the iron core and the test piece. Dirt or faulty dimensions may cause an air gap, which forms a very high resistance in the magnetic circuit. But if the end surface of the feeler core is of suitable shape and is properly maintained, this error can be kept within reasonable limits.

An advantage of the iron-cored coil is its suitability for measuring non-magnetic layers, such as lacquer coatings, foils, etc. (Fig. 9). For testing coatings on iron surfaces the same conditions must be fulfilled as for testing the iron itself. For measuring foils, a soft iron plate is used as a base. For routine tests of foils the maximum thickness is not specified but, whether the actual thickness lies between specified limits. Obviously a magnetic instrument may be calibrated for precise maximum or minimum measurements as well.

For the successful application of magnetic testing methods a certain experience is necessary, chiefly for choosing the right sensitivity. In general it is better not to exaggerate the sensitivity of the instruments, because the results would be too widely scattered.

Figs. 10 to 13 give examples of results obtained with moderate magnetic systems.

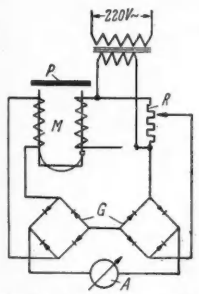
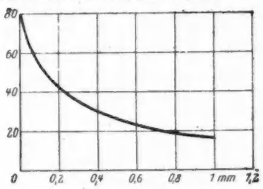


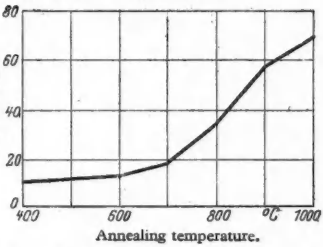

**Fig. 7. Iron tester with opposing voltage derived from a.c. mains, Transformer used as a feeler.** *A* Instrument, *G* Rectifier, *M* Feeler, *P* Test piece, *R* Resistance.

Graduation of Instrument Dial.




Thickness of layer.

Graduation of Instrument Dial.



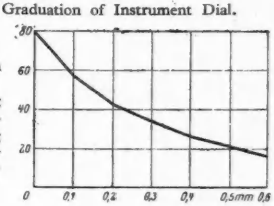

Annealing temperature.

**Figs. 10-13.** Some results obtained with magnetic methods.

**Fig. 10.** Measuring the thickness of a layer.

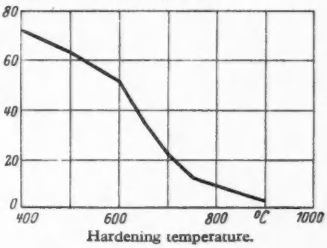
**Fig. 11.** Measuring the depth of a case-hardened layer on St. 42.11 steel.

Graduation of Instrument Dial.




Depth of case-hardened layer.

Graduation of Instrument Dial




Hardening temperature.

**Fig. 12.** Checking the annealing temperature of silver steel.

**Fig. 13.** Checking the hardening temperature of silver steel.

# SHORT-TIME RATING OF ELECTRICAL MACHINERY.

(From *ETZ*, Vol. 64, No. 25/26, 1st July, 1943, p. 354.)

In short-time and intermittent operation of electrical machines as opposed to continuous duty, considerably higher losses can be admitted, thus enabling the use of a correspondingly smaller machine for the same output. Heretofore, it has been general practice to determine the permissible increase in rating, and with it the permissible reduction in machine size, on the basis of the exponential law of temperature rise in time. For a certain length of time after starting, however, there exists a discrepancy between computed and actually measured rates of temperature rise. This disagreement is explained by the fact that the machine cannot be considered a homogenous body, being composed, as it is, of copper and iron; while cooling air must be considered a third component of the system subjected to heating.

The temperature increase of the copper can be derived from the expression $\vartheta = \Theta_1 (1 - e^{-t/T_1}) + \Theta_2$

$(1—e^{-t/T_2})$ representing two superimposed experimental functions. In this equation, under certain conditions, $\Theta_1$ may be taken as final temperature difference between copper and iron, with $\Theta_2$ representing that prevailing between iron and copper. Actual measurements have shown $\Theta_1$ to lie in the range of 0.2 $\Theta$ to 0.35 $\Theta$, and $\Theta_2$ between 0.8 $\Theta$ to 0.65 $\Theta$, the final copper temperature being given by $\Theta = \Theta_1 + \Theta_2$; while the time constants $T_1$ and $T_2$ vary from 6–10 m and 20–40 m respectively. Since in most cases, $T_1$ will be considerably smaller than actual operating time, the final copper temperature at the end of the operating time can be expressed with sufficient accuracy by $\vartheta \sim p\,\Theta_1 + p\,\Theta_2\,(1—e^{-t/T_2})$.

The validity of this equation rests on the proviso that in short time operation, both copper and iron losses may be p times the respective losses permissible in continuous operation. If the temperature rise in short time duty is not to exceed the temperature $\Theta$ obtaining in continuous operation, $\vartheta$ may be made to equal $\Theta$ and it can be written $p = \dfrac{1}{1—\dfrac{\Theta_2}{\Theta}\,e^{-t/T_2}}$, which means that short-time rating may be p times continuous rating. Similarly, if copper losses alone may be increased, the maximum short-time rating can be shown to be limited to $\sqrt{p}$ times continuous rating. But these are extreme limits between which most actual cases will be found to lie.

In most cases, totally enclosed machines will permit of a considerable increase in magnetization without exceeding permissible saturation; and it will therefore be admissible to rate them p times continuous rating. In machines of the open and the protected types, the permissible increase in magnetization will be small. This short-time rating will therefore be limited to $\sqrt{p}$ continuous rating. This method of rating is also applicable to cases in which the intervals between operating periods are too small to permit complete cooling down, so that a copper temperature $\vartheta_a$ prevails at the commencement of the next operating period.

This same method can also be used to determine permissible length of time for which a normally rated machine may be operated at ambient temperature above normal. With the introduction of further simplifications, the use of this method can be extended to the computation of final temperature rises and admissible increases in rating in cyclic operation, and also to any given variations in operating duty.

As a rule, cases of this kind are based on the R.M.S. value of the loading, providing that the ratio of operating period to duration of operating period and interval is low, and that the machine temperature changes but slightly during the operating cycle. The smallest permissible size of machine can be found by trial and error. The figure obtained for short-time rating will be found to lie considerably above the rated continuous output. No claim is made for a strict accuracy of the method.

As the introduction of $\Theta_1$ and $\Theta_2$ as copper and core temperatures respectively constitutes the largest source of error, it may be appropriate to employ temperatures different from those indicated by the curves of temperature rise. Under certain conditions, the method described may yield values quite different from those obtained with the methods hitherto used.

# ROLL PRESSURE MEASUREMENT BY MEANS OF AN ELECTRIC STRAIN GAUGE.

By D. W. REDEPENNING. (From *V.D.I.-Zeitschrift*, Vol. 87, No. 17-18, May 1st, 1943, pp. 265-266).

MEASUREMENT of the rolling pressure is important for several reasons. Thus, apart from other considerations, maintenance of a certain rolling pressure is necessary for the sake of controlling the physical properties of the final rolled product. Also, determination of the pressure exerted by the rolls of the mill with different conditions of rolling affords the gaining of additional insight into the behaviour of the metal during the process of deformation undergone by the material. Finally, a knowledge of the forces prevailing in the roll gap is essential to an effective supervision of the loads imposed upon rolls and roll stand.

In previous practice, rolling pressures were measured by means of pressure gages of the quartz or of the electric condenser type; or else they were based on the magneto-elastic principle. Measuring instruments for such purposes should not generally exhibit any inertia effect; and should show constancy in service without requiring frequent calibration. Nor should they be subject to a deformation larger than that of the stressed parts of the mill, or be influenced by temperature variations. Finally, they should make it possible to obtain continuous graphic records under the rough conditions prevailing in roll mill operation. These requirements are met by a novel measuring device, the operating principle of which consists in measuring the strain of the roll stand caused by the roll pressure. These strain values will always lie within the proportionality range of the material of which the roll stand is made. Therefore the relationship prevailing between strain and roll pressure can be computed on the basis of Hooke's law, by taking into account both the modulus of elasticity of the material and the cross sectional area of the stand; it can of course also be found experimentally by comparative measurements.

Measurement of the strain is conducted over a conveniently located gaging distance of e.g., 500 mm. in length. The elastic elongation of the material over the gaging distance will be in proportion with the roll pressure, and its actual magnitude can then be determined by comparison with a standard length represented by the unstressed measuring rod 'a' shown in Fig. 1. The free end of the measuring rod

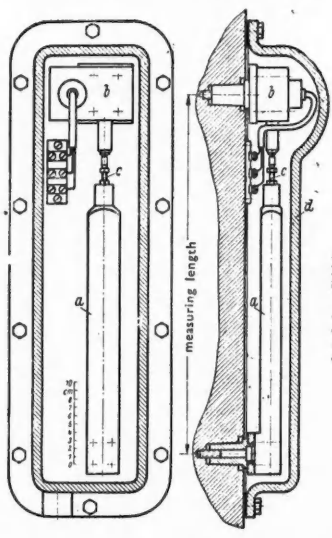



**Fig. 1. Arrangement of roll pressure measuring gage on the roll stand.**
*a* Measuring rod
*b* Feeler
*c* Adjusting screw
*d* Protective housing

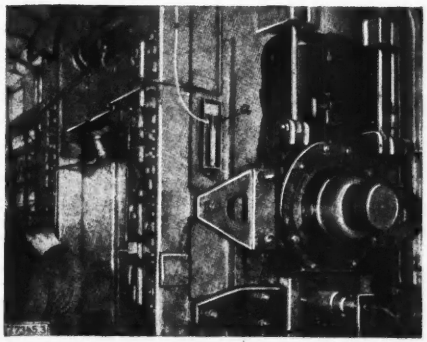


**Fig. 2. Roll pressure measuring gauge *a* on a roll stand.**

actuates the electric measuring device 'b,' the reaction of which is communicated to an electrical recorder. This arrangement, therefore, permits electrical recording of the strain, and with it of the roll pressure, at any convenient distant point or points. An optical or acoustical alarm device can also be fitted, which goes into action whenever the maximum permissible roll pressure is exceeded.

The fitting of this measuring device to a mill is a very simple matter, as it is only necessary to drill two holes in the mill stand, the distance between the holes constituting the measuring length. The adjusting screw 'c' shown in Fig. 1 permits the adjusting of the value of elongation registered by the instrument at a given roll pressure to that theoretically computed. In effecting calibration of the instrument on the basis of theoretical strain computation, the following points must be noted:

(1) The strain caused by a given roll pressure will frequently be unevenly distributed over the cross-sectional area of the roll stand. It is. therefore, desirable to arrange the gauging distance along the neutral axis of the stand. Where this proves impossible, the error incurred can be computed or it can be ascertained empirically.

(2) Experience has shown that computation of the cross sectional area of the stand from the shop drawing of the casting may involve an error of the order of plus or minus one per cent, because of a deviation of actual dimensions from those specified in the drawing.

(3) The modulus of elasticity is influenced by both composition of the material and by the casting process proper, and absolutely correct values cannot be given frequently. In such cases, an error of plus or minus five per cent must be allowed for in the establishment of the recorded elongation.

Temperature influences will be negligible if the measuring rod is covered by a suitable housing. Owing to its small thermal capacity as compared to that of the stand and also to its proximity to the gauged length, the temperature of the measuring rod will follow that of the stand without lag. With an instrument error of one per cent, the total error will not, therefore, exceed plus or minus seven per cent if calibration is effected by computation; and it will be reduced to only one per cent if the calibration is based on a standard instrument, should such be available.

# A NOVEL METHOD FOR SHARPENING RAGGED AND KNOBBLING ROLLS.

By O. RADEMACHER. (From *Stahl und Eisen*, Vol. 63, No. 22, June 3rd, 1943, pp. 446-447).

IN steel mill practice, the sharpening of ragged rolls used to be a time wasting and expensive piece of work which could only be executed by hand or by compressed air chisels. Work of this kind had to be done by skilled workers, in order to ensure uniformity of sharpening and proper contour of the depressions.

On the basis of a few experimental installations, the introduction of special milling machines and lathes for roll-sharpening was proceeded with on a rather large scale, thus replacing manual work by machine operation. In recent years, however, the limitations inherent in the milling process, and the requirements of specially shaped milling cutters, as well as the need for highly skilled machine operators, have led to the preponderant employment of roll sharpening lathes. Outstanding points in their favour are shorter machining times and the possibility of dressing knobbling rolls as well. Still another inherent advantage derived from the use of the lathe is that ordinary cutting tools can be used, provided the distance between the projections is not less than four to five times the thickness of the projecting ridges.

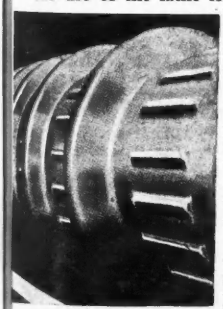


Fig. 1. **View of a Knobbling Roll.**

In the last few years, a few blooming mills have been employed with knobbling rolls in the first pass, as shown in Fig. 1; the exact method of their production being dependent upon the roll diameter. For knobbling rolls with clearances less than 1.5 times the peripheral width of the projections, the use of a roll sharpening lathe becomes impossible, and a mill cutter must be employed. In the latter case, the depth of the cut approximates to h=4-6 mm., as indicated in Fig. 2. Care must be taken not to cut below the profile diameter, to make the radius 'r' sufficiently large, and to avoid the formation of sharp edges, in order to prevent the adherence of scale. Knobbling rolls with small clearances between the projections are used for the purpose of effecting breaking of the scale to the greatest possible extent, and for increasing the biting power of the rolls, and with it the reduction per pass.

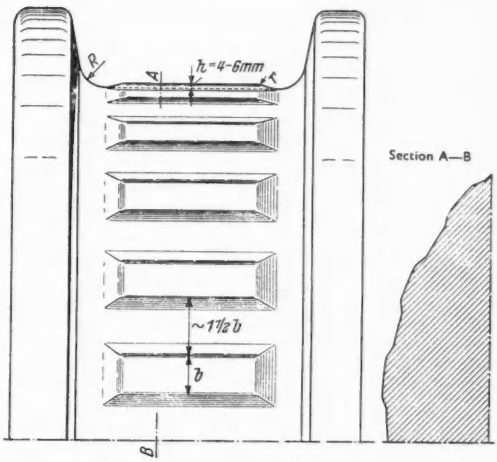



Fig. 2. **Dimensions of the Projections of a Knobbling Roll.**

# DRESSING GRINDING WHEELS WITHOUT DIAMONDS

By Von Heinz Frank, Schmalkalden. (From "*Werkstatt und Betrieb*," Vol. 76, No. 3, March, 1943, pp. 49-50).

Dressing tools without diamonds have been found very useful for dressing grinding wheels of surface grinders, tool grinders, cylindrical grinders, etc.

A very good dressing tool comprises a double disc (Fig. 1) mounted in a holder. By the use of this double disc the wheel is dressed uniformly both at the centre and at its two edges, the speed of the double disc remaining constant. One of the discs is always in contact with the wheel and during a change of direction the speed of the double disc is not reduced. The edges of the wheel are, therefore, no rougher than at the centre and the diameter at the edges is no more reduced than that at the centre (Fig. 2).

The tool feed during the dressing operation is very small (0.01 to 0.03 mm.). This is sufficient to make the double disc rotate at the same circumferential speed as the grinding wheel. The double disc is mounted in ball bearings, thus ensuring smooth running which is an essential condition for satisfactory dressing.

For specially high surface quality the double disc is replaced by a single slotted disc (Fig. 3). In this case also the edges of the grinding wheel are kept in good condition, because during a change of direction the pressure between tool and wheel is maintained through the part of the disc which remains in contact with the wheel.

With the double disc and its special mounting, accurate grinding and perfect surface finish are achieved; the results obtained being equivalent to those obtained with wheels dressed with diamonds.

The wear on the disc is very small, and is caused only by the shearing force required to break off a crystal of the grinding wheel. The grinding effect of the wheel on the disc is neutralized, the disc rotating at the same speed as the grinding wheel, from the moment of contact till the end of the dressing operation.

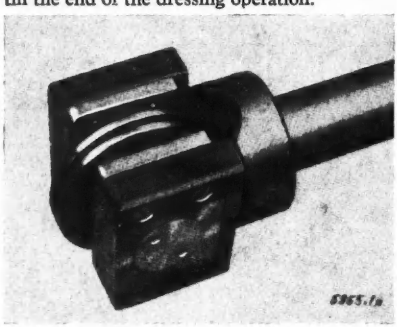


Fig. 1a. Dressing tool with double disc.
Fig. 1b. (Right) Double disc for dressing tool.

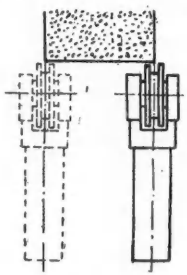


Fig. 2. Double disc in operation.

With surface grinders having a horizontal grinding wheel spindle, the dressing tool, mounted on a base plate, is fixed to the bed of the machine and the axis of the double disc must be parallel to the spindle of the grinding wheel (Fig. 4).

Surface grinders with a vertical grinding wheel spindle, have the head of the dressing tool screwed to a base plate of simple type fixed to the bed of the machine, which moves in a longitudinal direction. In the case of a rotating table a special support is required.

Astonishing results were obtained in the surface examination, of a large number of case hardened steel bolts, by means of Trentini's instrument The bolts were measured at the ends and at the centre. One part of them had been ground with a wheel dressed with a diamond, the other with a wheel dressed with the "Atlantic" dressing tool, i.e., without diamond.

The results shown in tables 1a and 1b illustrate that the surface quality obtained with the "Atlantic" dressing tool is superior to that obtained with a diamond tool.

Table 1a, Specimen A, Material : St. 16.61.

| Grinding Time in min. | "Atlantic" roughness Trentini units in $\mu$ | | Diamond roughness Trentini units in $\mu$ | |
|---|---|---|---|---|
| 0.5 | 22 | 1.1 | 20 | 1.0 |
| 1 | 20 | 1.0 | 20 | 1.0 |
| 1.5 | 20 | 1.0 | 20 | 1.0 |
| 2 | 14 | 0.7 | 15 | 0.75 |
| 3 | 13 | 0.65 | 15 | 0.75 |
| 3.5 | 14 | 0.70 | 14 | 0.70 |
| 4 | 12.5 | 0.63 | 14 | 0.70 |
| 4.5 | 12.5 | 0.63 | 14 | 0.70 |
| 5 | 10.5 | 1.53 | 12 | 0.60 |

Table 1b, Specimen B.

| Grinding time in min. | "Atlantic" roughness in $\mu$ | Diamond roughness in $\mu$ |
|---|---|---|
| 1 | 1.20 | 1.25 |
| 1.5 | 1.15 | 1.25 |
| 2 | 1.15 | 1.15 |
| 2.5 | 1.05 | 1.15 |
| 3 | 1.00 | 1.10 |
| 3.5 | 0.95 | 1.05 |
| 4 | 0.95 | 1.05 |
| 4.5 | 0.90 | 1.05 |
| 5 | 0.80 | 0.90 |

Note : The roughness was measured by means of the Trentini instrument and expressed in $\mu$.

A similar picture is obtained from comparative experiments in which bolts were roughly ground and then ground to a superfinish. Table 2 shows the gradual improvement of the surface, with an increasing number of work pieces per dressing operation when using a dressing tool without diamond. It will be noticed that a comparatively high degree of roughness was obtained with the diamond. The dressing opera-

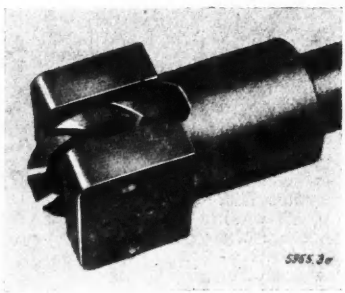


Fig. 4.
Dressing tool mounted on base plate for surface grinders with horizontal spindle.

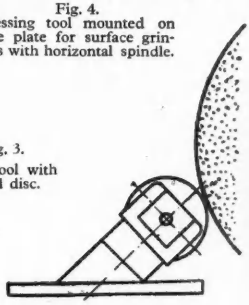


Fig. 3.
Dressing tool with slotted disc.

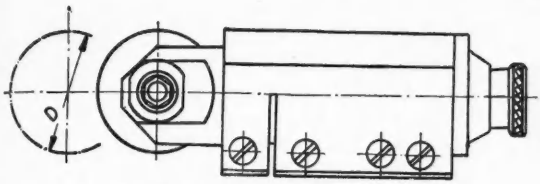


Fig. 5. Dressing tool with fine feed adjustment

tion was purposely carried out with a high feed on both tools. The object of this, was to show that even with such a high degree of roughness, a very high degree of accuracy could be obtained in the following finish-grinding operation. This accuracy was roughly 0.5$\mu$ with the diamond, and 0.2$\mu$ with the tool without diamond, an accuracy that was hardly measurable.

Even with very short grinding times, the surface quality achieved was better than that obtained with grinding wheels dressed by means of diamond tools. Besides this it must be borne in mind that the following super-finish grinding is quicker on bolts which had been roughly ground with wheels dressed without diamond, and that a higher degree of final surface finish is obtained.

Table 2.

| No. off. | 2 | 4 | 6 | |
|---|---|---|---|---|
| Diamond Grinding time 3 mins. | 65..70 4..4.5 | 65..70 4..4.5 | 65..70 4..4.5 | Trentini roughness Roughness expressed in $\mu$ |
| Atlantic Double disc Grinding time 2-3 mins. | 30 1.8 | 20..30 1.2..1.8 | 10 0.6 | Trentini roughness Roughness expressed in $\mu$ |

Actually, these viewpoints are perfectly intelligible, as the grinding wheel has a completely different surface structure when dressed with diamond, steel disc, or a silicon-carbide disc. The surface structure obtained with the diamond is more open. Both feeding pressure and surface finish of the part to be ground depends on the surface structure of the wheel.

There are various types of steel discs used for dressing tools mentioned above to suit the different surface finishes required. A slotted steel disc of 4 mm. width, or a set of steel discs with a total width of 4 to 8 mm., do not give coarse grinding wheel surfaces, but result in a better surface finish which can be regulated by using a correct steel disc in the dressing tool.

It is desirable to know to what extent the dressing

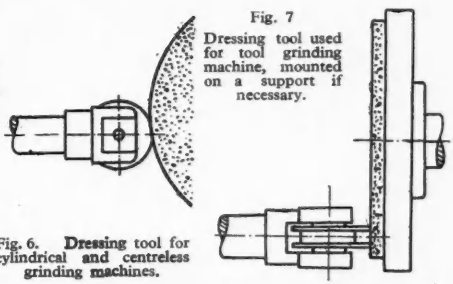


Fig. 7
Dressing tool used for tool grinding machine, mounted on a support if necessary.

Fig. 6. Dressing tool for cylindrical and centreless grinding machines.

operation is influenced by the increased feeding pressure cause by the width of the dressing tool, i.e., to what extent the wear of the edges of the grinding wheel is increased, and by how much the sharpness of the wheel is reduced by the width of the dressing tool, the number of pieces ground per dressing operation obviously depending on the sharpness of the wheel.

The number of grinding operations per dressing operation is, therefore, higher with a dressing tool of smaller width.

With narrow dressing tools, a higher wear of the grinding wheel is caused. This, however, can be reduced by using harder grinding wheels of a firmer grain or texture.

The surface finish obtained on two specimens ground with the same wheel, one dressed with a narrow steel disc, the other dressed with a wide disc, cannot be compared. Each dressing tool must be used for a definite type of wheel, then the same degree of surface finish and the same wear of the grinding wheel can be obtained.

The correct handling of the dressing tool is very important. It must be rigidly fixed and perfectly aligned. It is advisable to work with a slow lateral motion, increasing the feed at the beginning to reduce the wear, and to reduce the feed gradually until, after about five cuts, the taper of the grinding wheel is removed and a cylindrical grinding surface, parallel to the grinding wheel spindle is obtained.

To obtain an easily adjustable feed the arrangement shown in Fig. 5 is often used. The smoothness of the grinding surface can be increased by pressing a hardened metal plate against the wheel. The dressing tools can be used for the cylindrical and the end faces of the grinding wheel (Figs. 6 and 7).

## GRINDING OF TOOLS.

Appliances for the manufacture of dies and form tools. By KARK LÜDTKE, Berlin. (From *Werkstattstechnik, Der Betrieb*, Vol. 37/22, No. 5, May, 1943, pp. 185-188).

*Grinding of hardened Blanking Punches.*

BLANKING punches were generally hand finished by the toolmaker, having been shaped or milled beforehand. If during the hardening process the punch cracked then valuable machining and working time was lost, as well as material wasted. Although cracking does not occur frequently, one must consider the possibility of distortion of the punches during hardening.

Fig. 1

Very often, much time is spent in rectifying distorted punches and it depends largely on the skill of the toolmaker whether the requisite accuracy of dimensions can be obtained ultimately.

To overcome this difficulty a process was developed, by which it is possible to grind the punches to the required shape (Fig. 1) immediately after machining and hardening. A working allowance for grinding averaging from 0.3 to 0.4 mm. was given. Two methods were used for grinding the punches and which are complimentary to one another. In the first, the grinding wheel is dressed to the required profile by means of a diamond tool ; in the second, the punch is adjusted to the desired position relative to the grinding wheel. Both methods require a number of fixtures; these are described below.

A time table (Fig. 2) is very often used to set angular positions with high precision. To set the table to a given angle, one roller is packed up with a slip gage, the size of which is determined by the product of the distance between the two rollers and the sine of the

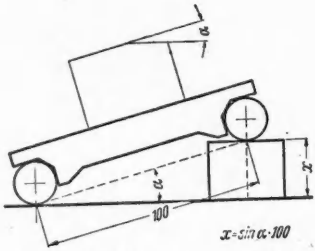



Fig. 2

angle. The centre distance between the rollers has been made 100 mm., in order to facilitate toolroom calculations.

*Profiling grinding wheels.*

The fixture for dressing grinding wheels to the desired profile is shown in Fig. 3. The base plate *a* is a T-shaped iron. A sine gage is fixed by a bolt through an oval hole to the base plate and a slide block *d* carrying the diamond holder, slides on it and is actuated by a lever *f*. The angle between the lower edge of the base plate and the slide block is 45 degrees. The illustration shows how the tool can be set to a pre-determined angle by packing one of the rollers with parallels. In Fig. 4 an arrangement is shown by means of which the grinding wheel circumference can be dressed to a profile of given radius. If the point of the diamond tool is beneath the axis *h* the wheel edge is dressed convex, if it is above the axis, a concave profile is obtained. To set this apparatus, the diamond is clamped vertically to the axis of the diamond holder. The adjusting gage is placed into a slot provided in the upper part of the axis carrier, and indicates the distance

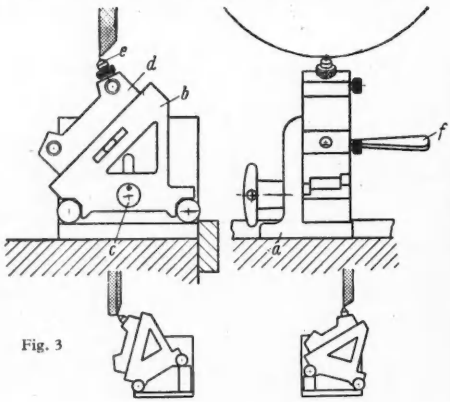



Fig. 3

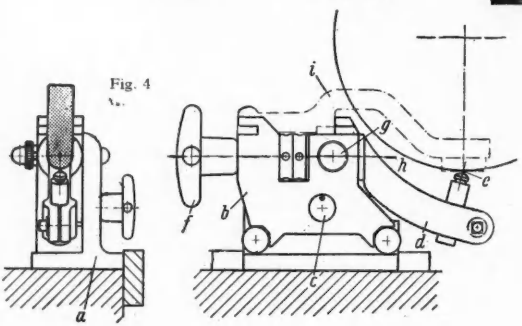



Fig. 4

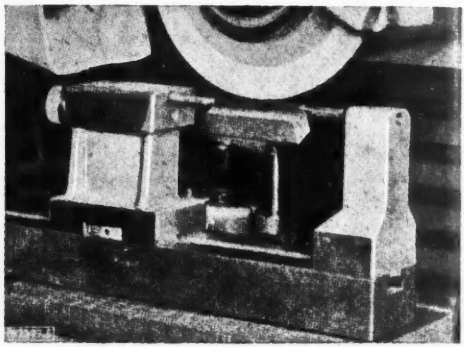


Fig. 5

between the lower edge of the axis carrier and the axis.

The sine gage is also used for grinding tools which enclose an angle with the grinding wheel. One of its rollers serves as a hinge and also for fixing the gage when the correct position is set by placing packing under the other roller.

A tailstock is used as shown in Fig. 5 for grinding punches with cutting edges composed of straight lines and arcs. The tool is centered before hardening, then fixed between the two centres. During grinding radii arc produced by rotating the tool between the centres.

*Grinding with the use of templates.*

Templates are used for grinding irregularly shaped punches. To obtain great precision, the outlines of the template are projected, magnified ten times on to a screen, which has a drawing of the required contour of the cutting edge, with the same magnification fixed to it. The finished template is then soldered to the top

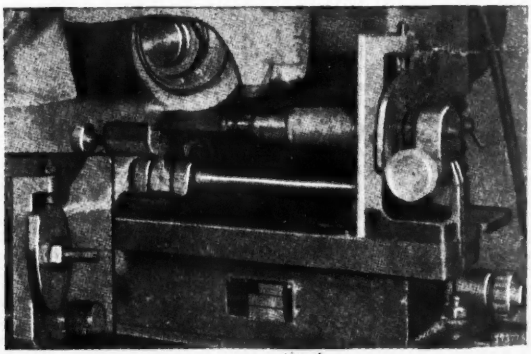


Fig. 6

face of the punch, clamped to one of the fixtures described above and the punch is then ground to the shape of the profile.

*Copy-grinding.*

Copy-grinding is recommended only when several punches with irregular profile are to be ground, or for punches whose outside dimensions differ only by a fraction of a mm. The template required for this grinding process should be checked against a 'master' with a ratio of 10 to 1. The copy-grinding apparatus shown in Fig. 6 consists principally of a free-swinging, fork-shaped, centre engaging piece which is connected by an interposed joint to the base plate. A parallel portion of the centre engaging piece is secured to the magnetic free plate by a double bucket. Side-slip is

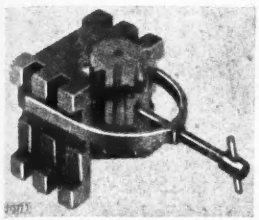


Fig. 7

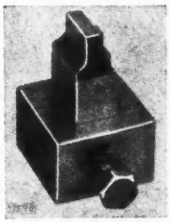


Fig. 8

obviated by a vertical guide. An adjustable centre is attached to one side and a bearing carrying the spindle is fixed to the other. The template is fastened on the extreme end of the spindle and on the inner end there is a holder for the punch. The entire mounting, complete with the template, is held against a cam by means of a spring and when the punch is ground to shape the template is removed.

*Grinding of rectangular surfaces.*

In order to grind the top surface of punches at right angles to the side surfaces already ground, jigs as shown in Fig. 7 are used. These jigs are of course, made with great precision because the manufacturing accuracy of the tool depends on them.

*Bending brake tool punches.*

To grind punches for bending work a dog clamp is usually employed as shown in Fig. 8. This clamp has a hole bored through its centre, into which the shank of the punch is placed and held tight by a bolt. The side surfaces of the punch can thus be ground parallel to the shank and at right angles to each other and at right angles to the axis of the shank. Should it be necessary to grind the top surface of the punch at any angle to the axis of the shank, then the jig is placed on one of the devices with the sine plate attachment.

*Ground dies.*

A die is usually hand finished first, then it is hardened

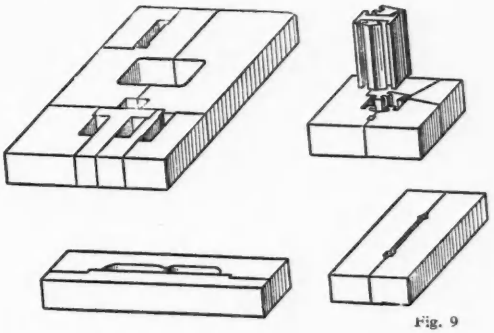


Fig. 9

and any distortion which may occur is rectified by stoning. Therefore, wherever a sub-division of die-plates is necessary, ground dies are recommended. Compound and ground dies offer great advantages over many follow-on and blanking tools. It is sometimes essential that dies are sub-divided into single parts and fitted into a frame indicated in Fig. 9. Each die part is accurately ground to size after hardening and then assembled together. By this means dies of high accuracy are obtained, which require a clearance of only $\frac{1}{4}^{\circ}$, whereas filed dies have usually a clearance of $\frac{1}{2}^{\circ}$.

*Wear of grinding wheels.*

At first sight it may appear that due to profiling of wheels, a great number of grinding wheels must be kept constantly in use, or that because re-dressing the profiles causes great grinding wheel wastage. Ten years' experience has, however, proved that these assumptions are not justified. It has been found possible to alter existing wheel profiles with little cost and in some factories certain profiles are required over and over again on repetition work.

# GERMAN MACHINE TOOLS

## CYLINDRICAL THREAD GRINDING MACHINE

(From *Die Werkzeugmaschine*, Vol. 46, No. 10, May, 1942, pp. 297-298).

By means of a thread surface-grinding fixture, threads or profiles can be ground on straight surfaces, i.e., screw dies, sets of thread-gages, micrometer parts and racks. The fixture offers a wide range of application to the cylindrical grinding machine. The width of the

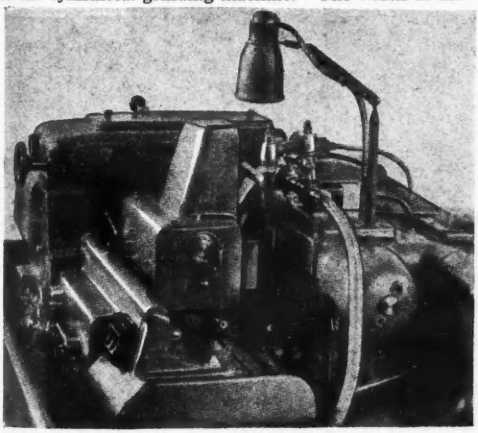


Fig. 1

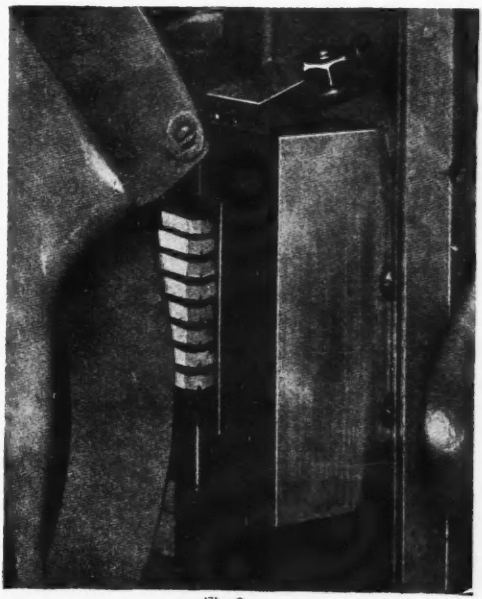


Fig. 2

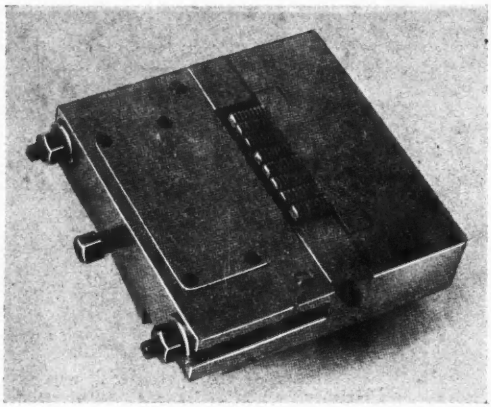


Fig. 3

grinding wheel and diameter, the single or multi-profiled grinding wheel, the forming apparatus for these wheels, the working capacity with regard to sizes, types and forms of threads, cooling mechanism, driving power, etc., are exactly the same as on the cylindrical thread-grinding machine. The arrangement of the attachment, mounted on the thread-grinding machine, is shown in Fig. 1. Fig. 2 shows the grinding of radial screw-thread dies, by means of a thread surface grinding fixture on the thread grinding machine. A special clamping device is shown in Fig. 3 for two sets of radial screw-thread dies and a universal clamping device for the grinding of tangential and radial screw-thread dies in Fig. 4.

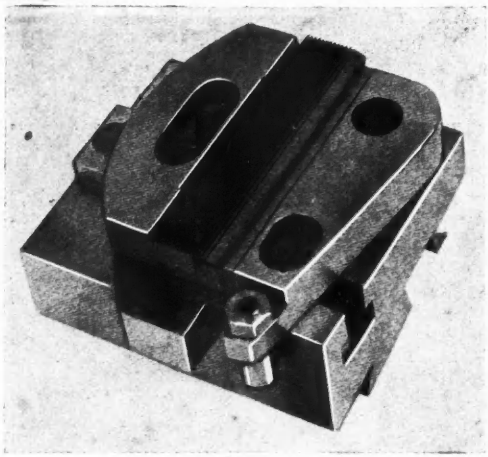


Fig. 4

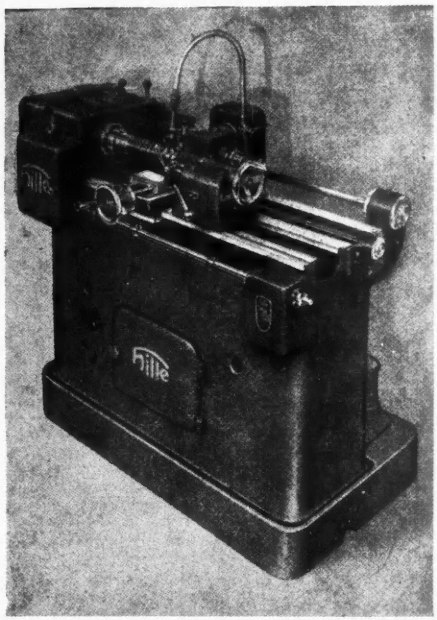
The apparatus is driven in a simple manner through a transmission from the work-spindle and with the use of the speed regulator, speeds of 12-470 r.p.m. can be reached. The length of the stroke is adjustable between 30 and 130 mm. Re-setting the stroke is automatic. The thread surface grinding necessitates the use of the existing dividing apparatus for the axial feed of the grinding table, so that a further dividing in the pitch can be obtained. For economic grinding it is necessary to provide the machine with a multi-surface grinding wheel.

Radial screw-thread dies can either be ground singly or by clamping them in cascade making it possible to grind several together. With tangential screw-thread dies clamping of more than one is impossible and these can only be ground singly.

## THREAD AND GEAR-HOBBER.

(From *Die Werkzeugmaschine*, Vol. 46, No. 10, May, 1942, pp. 303-304).

MILLING of small threads requires the utmost attention. The milling machine shown in Fig 1 is suitable for milling small threads of 2-75 mm. dia., 500 mm. length and a pitch of 0.2-18 mm.



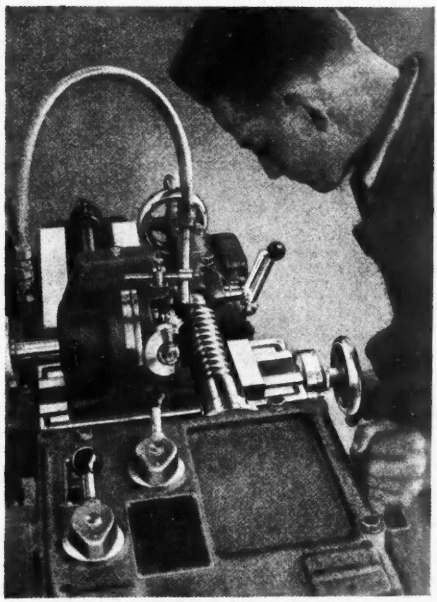


Fig. 1 (top) Fig. 2 (bottom)

Single or multi-start, outside or inside threads can also be hobbed by means of a disc shaped single shaft miller. Short outside and inside threads can be manufactured with cylindrical-shaped, thread milling cutters, in accordance with the short thread hobbing process by only one revolution of the workpiece. Grooves can be produced, after either the single or short-thread hobbing process, with disc or cylindrical shaped hobbers. In Fig. 2, a hobber support of very rigid design is shown, to ensure that during the heaviest cutting operations the machine is shock proof and satisfactory working of the hobbing tool is accomplished.

To set the hob-spindle bearing, according to the pitch angle of the thread to be hobbed, a divisional scale bearing is provided, the vernier of which is situated on the hob-saddle. Transverse movement of the hob-saddle is by means of the thread spindle. Threads can be hobbed accurately to 0.01 mm. in depth. A special appliance with worm and wormwheel is provided for fine adjustment of the entire hob support.

The machine has a self acting stop mechanism which is operated by the cylindrical shaped hobbers. If special accuracy is required for a job a precision guide-spindle is supplied.

For some operations special equipment must be employed. Fig. 3 shows a compensating apparatus, used for hobbing threads on which the pitch distances were distorted during hardening. A further attachment is shown in Fig. 4, employed for hobbing threads of conical section. It consists of 2 tailstocks and a guide straight edge which can be swung right or left for 20 mm.

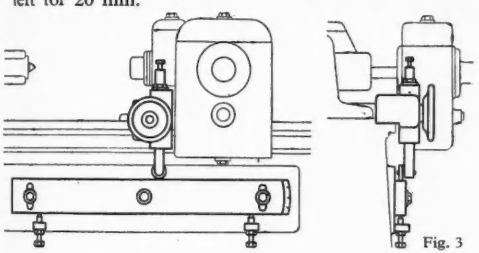


Fig. 3

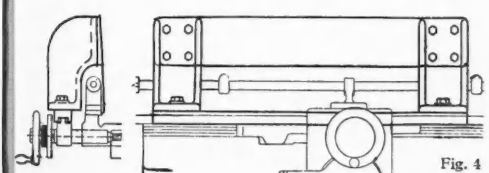


Fig. 4

## DOUBLE CUTTER—BEVEL-GEAR—SHAPER.

(From *Die Werkzeugmaschine*, Vol. 46, No. 10, May, 1942, pp. 298-299).

THE tool used for the manufacture of bevel-gears in generating-shaping-machines has a straight profile and regrinding is, therefore, greatly simplified; the profile disposition requires only slight alteration in the setting of the machine. While the machines are mainly intended for cutting helical bevel-gears, they can also be used for cutting spiral teeth. The gear ratio between the dividing head spindle and the tool is accurately determined by change-wheels, so that each pair of bevel-wheels has the correct generating ratio. A small model of this machine is illustrated in Fig. 1; in this machine the changing of gears is dispensed with. Transmission is obtained by altering the point of application of a lever on a vernier-scale.

As shown in Fig. 2 the double cutter bevel-gear shaping-machine has two saddles with a cutter fastened in each. During roughing, the bottom cutter penetrates into the job first. In the following operations, the top cutter engages the crest of the upper tooth surface of the same tooth, thus generating both involute tooth surfaces. After the generating operation, the job and the dividing head spindle move back quickly, until the cutters are clear of the cut. The gear then turns through another division and the cycle is repeated. During every backward stroke the cutters are withdrawn from the job, this in order to preserve the cutting edge and to prevent injury to the tooth surface just ground.

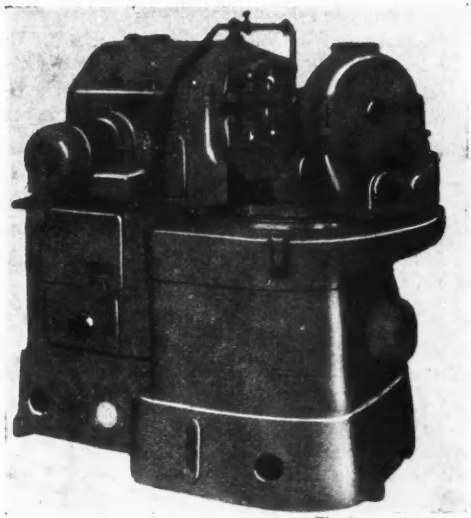


Fig. 1

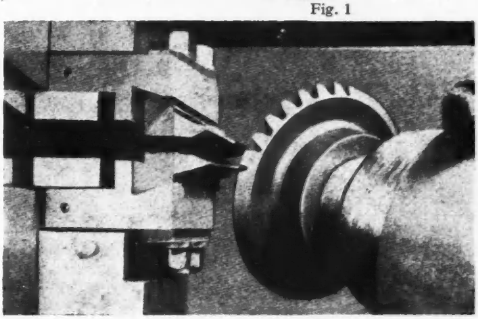


Fig. 2

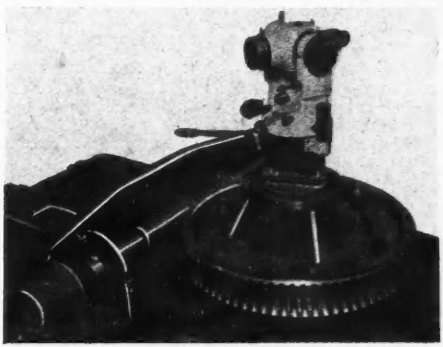


Fig. 3

Fig. 3 illustrates the inspection of the pitch of a

# SOME SIMPLE FORMULÆ OF THE THEORY OF AEROFOILS.

By H. B. HELMBOLD, Munich. REPORT from the Design Department of the Heinkel Aircraft Works, Rostock-Marienehe. (From *Luftfahrtforschung*, Vol. 19, No. 10-12, January, 1943, pp. 363-366.)

## 1. Fundamentals.

If the plan form of the wing be expressed by the ration of local chord to center chord $\frac{t(\xi)}{t(0)} = \frac{t}{t_0}$, and the (geometric) angle of incidence $\alpha(\xi)$. $\xi$ is the distance from the wing centre line in terms of the semi-span b/2, and $\alpha$ the angle between zero-lift and flight directions. There is proportionality between $c_a(\xi)$, the local lift co-efficient and $\alpha_p(\xi)$ the local angle of incidence: $c_a = c'_{ap}\,\alpha_p$. Hereby, $\alpha_p = \alpha - \alpha_i$, $\alpha_i$ being the induced angle (angle of downwash). The integral equation for the monoplane is

$$\alpha(\xi) = \alpha_p(\xi) + \frac{(c'_{ap})_0\, t_0}{4\pi b} \int_{-1}^{+1} \frac{d}{dx}\left(\alpha_p \frac{n\,t}{t_0}\right) \frac{dx}{\xi - x} \quad .. \quad (1)$$

with $$n = \frac{c_{ap}(\xi)}{c'_{ap}(0)} = \frac{c_{ap}}{(c'_{ap})_0}.$$

To solve it we have (see Prandtl, Tragfluegeltheorie I, 1918, or reprint, 1927, p. 31)

$$\alpha_p(\xi) = \frac{c_{a0}}{c'_{pa}(0)} \quad \frac{t_0}{nt}\sqrt{1-\xi^2}\,(1 + k_1\xi^2 + k^2\xi^4 + \ldots) \quad (2)$$

and obtain for the induced angle

$$\alpha_i(\xi) = \frac{c_{a0}\, t_0}{4b}\left[1 - k_1\left(\tfrac{1}{2} - 3\xi^2\right) - k_2\left(\tfrac{1}{8} + \tfrac{3}{2}\xi^2 - 5\xi^4\right)\right] \quad (3)$$

for the lift co-efficient of the whole wing (area F)

$$\bar{c}_a = \frac{b\,t_0}{2F}\int_{-1}^{+1} c_a \frac{t}{t_0}\,d\xi = \frac{\pi}{4} c_{a0} \frac{b\,t_0}{F}\left(1 + \frac{k_1}{4} + \frac{k_2}{8}\right) \ldots (4)$$

and for the co-efficient of induced drag

$$\bar{c}_{wi} = \frac{b\,t_0}{2F}\int_{-1}^{+1} \alpha_i\, c_a \frac{t}{t_0}\,d\xi =$$

$$= \frac{\pi}{16}\frac{(c_{a0}\,t_0)^2}{F}\left(1 + \frac{k_1}{2} + \frac{k_1^2}{4} + \frac{11}{32}k_1 k_2 + \frac{k_2}{4} + \frac{9}{64}k_2^2\right) \quad (4a)$$

## 2. The Untwisted Wing.

If the zero lift directions of all wing sections are parallel, $\alpha = \text{const.} = \bar{\alpha} = \alpha_p(0)$, or because of eq. (2) and eq. (3)

$$\bar{\alpha} = \frac{c_{a0}}{(c'_{ap})_0} + \frac{c_{a0}\, t_0}{4b}\left(1 - \frac{k_1}{2} - \frac{k_2}{8}\right) \quad .. \quad (5)$$

Therefore, with eq. (2) the local lift co-efficient becomes

$$c_a(\xi) = \frac{\alpha_p}{\bar{\alpha}}\, c'_{ap}\,\bar{\alpha} =$$

$$= \frac{n\,t}{t_0}\sqrt{1-\xi^2}\;\frac{1 + k_1\xi^2 + k_2\xi^4}{1 + \frac{(c'_{ap})_0\, t_0}{4b}\left(1 - \frac{k_1}{2} - \frac{k_2}{8}\right)}\, c'_{ap}\,\bar{\alpha} \quad (6)$$

Another form is obtained when requiring $\alpha_p(\xi) + \alpha_i(\xi) = \bar{\alpha}$. Again, with eqs. (2) and (3)

$$c_a(\xi) = \frac{c'_{ap}\,\bar{\alpha}}{1 + \frac{\alpha_i(\xi)}{\alpha_p(\xi)}} = \frac{c_{ap}\,\bar{\alpha}}{1 + \frac{(c'_{ap})_0\, t_0}{4b}\frac{n\,t}{t_0\sqrt{1-\xi^2}}\frac{1 - k_1\left(\frac{1}{2} - 3\xi^2\right) - k_2\left(\frac{1}{8} + \frac{3}{2}\xi^2 - 5\xi^4\right)}{1 + k_1\xi^2 + k_2\xi^4}} \quad (6a)$$

The first equation leads to quicker result, the second to which it will be reverted later is of theoretical interest. The mean lift coefficient from eqs. (4) and (5) is

$$\bar{c}_a = \frac{\pi}{4}\frac{b\,t_0}{F}\;\frac{1 + \frac{k_1}{4} + \frac{k_2}{8}}{1 + \frac{(c'_{ap})_0\, t_0}{4b}\left(1 - \frac{k_1}{2} - \frac{k_2}{8}\right)}\, c'_{ap}\,\bar{\alpha} \quad .. \quad (7)$$

For the condition that $\alpha_p(\xi) + \alpha_i(\xi) = \alpha_p(0) + \alpha_i(0)$, and using eqs. (2), (3) and (5) one obtains

$$\frac{n\,t}{t_0} = \frac{1 + k_1\xi^2 + k_2\xi^4}{1 - \frac{(c'_{ap})_0\, t_0}{4b}\left[3\left(k_1 - \frac{k_2}{2}\right)\xi^2 + 5\,k_2\xi^4\right]}\sqrt{1-\xi^2} \quad (8)$$

(Similar forms are given by A. Betz in *W. F. Durand, Aerodynamic Theory*, Vol. IV., Div. J, p. 58, Berlin, 1935.) To simplify the case, $k_2 = 0$ is assumed, $k_1$ determined from eq. (8),

$$k_1 = \frac{\frac{n\,t}{t_0} - \sqrt{1-\xi^2}}{3\,\frac{(c'_{ap})_0\, t_0}{4b}\,\frac{n\,t}{t_0} + \sqrt{1-\xi^2}}\,\frac{1}{\xi^2} \quad .. \quad (9)$$

and the formula for the local lift coefficient according to eq. (6) found to be

$$c_a(\xi) = \frac{t_0}{nt}\sqrt{1-\xi^2}\;\frac{1 + k_1\xi^2}{1 + \frac{(c'_{ap})_0\, t_0}{4b}\left(1 - \frac{k_1}{2}\right)}\, c'_{ap}\,\bar{\alpha} \quad (10)$$

As $\frac{t_0}{n\,t}$ is always positive, $k_1$ can be between 1 and $\frac{4b}{3\,(c'_{ap})_0\, t_0}$.

The procedure is now as follows :—Calculate the right-hand side of eq. (9) and denote it $a(\xi)$. If this value is constant, eq. (10) represents the exact solution of the problem; if, however, $a(\xi)$ varies along the span, an approximation can be obtained by substituting $a(\xi)$ for $k_1$ in eq. (10), as long as $a(\xi)$ does not deviate too much from a mean value. In other words, the condition is that at the wing centre $\frac{n\,t}{t_0}\sqrt{1-\xi^2} \cong \xi^n$ where $n \gg 2$. Fig. 1 shows the assumed value according to eqs. (9) and (10) compared with the exact solution of the untwisted rectangular wing for an aspect ratio $\lambda = \frac{b}{t_0} = (c'_{ap})_0 \cong 5.5$.

How the method works is made clear in Fig. 2; physically it is sensible as long as the wing contour satisfies the following condition :

$$\frac{(1-\xi^2)^{3/2}}{1+3\,\frac{(c'_{ap})_o\,t_o}{4\,b}\,\xi^2} \leqslant \frac{t}{t_o} \leqslant \frac{1+\frac{4\,b}{3\,(c'_{ap})_o\,t_o}\,\xi^2}{\sqrt{1-\xi^2}}$$

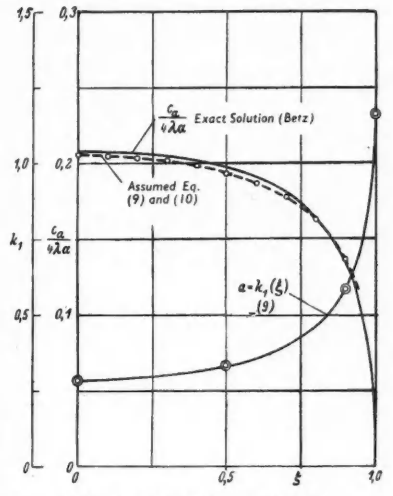



Fig 1. **Untwisted Rectangular Wing.**
Points shown thus *o* correspond to value $k_1$ ($\xi^*$) of Fig 2.

### 3. The Twisted Wing.

As eq. (1) shows a linear relation between $\alpha_p$ and $\alpha$, the problem of the twisted wing can be a split into a part proportional to $\bar{\alpha}$ and corresponding to the untwisted wing, and a part independent of $\alpha$ and due to twist, not contributing to lift. Thus, one can write, $\alpha(\xi) = \bar{\alpha} + \vartheta(\xi)$, and $c_a(\xi) = c_{a\alpha}(\xi) + c_{a\vartheta}(\xi)$; $\vartheta$ being the twist. $c_{a\alpha}$ has been dealt with in the previous chapter eqs. (6) and (6a). For the part due to twist one can write in accordance with eq. (2)

$$c_{a\vartheta}(\xi) = c'_{ap}\,\vartheta_p(\xi)\,c_{aI}\frac{t_o}{t}\sqrt{1-\xi^2}\,(1+k_I\,\xi^2+k_{II}\,\xi^4+\ldots) \tag{11}$$

and with eq. (3)

$$\vartheta(\xi) = \vartheta_p(\xi)+\vartheta_i(\xi) = \frac{c_{aI}\,t_o}{(c'_{ap})_o\,nt}\sqrt{1-\xi^2}(1+k_I\,\xi^2+k_{II}\,\xi^4)$$
$$+\frac{c_{aI}\,t_o}{4\,b}\left[1-k_I\left(\tfrac{1}{2}-3\,\xi^2\right)-k_{II}\left(\tfrac{1}{8}+\tfrac{3}{2}\,\xi^2-5\,\xi^4\right)\right] \tag{12}$$

Eq. (4) is replaced by

$$0 = \frac{\pi}{4}\,c_{a_I}\frac{b\,t_o}{F}\left(1+\frac{k_I}{4}+\frac{k_{II}}{8}\right) \quad .. \tag{13}$$

Herefrom, $k_{II} = -8\left(1+\frac{k_I}{4}\right)$. Assuming again $k_{II}=0$, one obtains from eq. (13) $k_I = -4$, and herewith from eq. (12).

$$\vartheta(\xi) = c_{aI}\left(\frac{t_o}{(c'_{ap})_o\,nt}\sqrt{1-\xi^2}+\frac{3}{4}\frac{t_o}{b}\right)\left(1-4\,\xi^2\right) \tag{14}$$

The total twist $\Delta\vartheta = \vartheta(0) - \alpha(0) - \alpha(1)$ is thus

$$\Delta\vartheta = \vartheta(0)-\vartheta(1) = \frac{c_{aI}}{(c'_{ap})_o}\left(1+3\lim_{\xi\to 1}\frac{t_o}{nt}\sqrt{1-\xi^2}\right)+$$
$$+3\,c_{aI}\frac{t_o}{b} \quad .. \tag{15}$$

Thus, $\vartheta(\xi)$ can be found as from the above equation $c_{aI}$ is determined.

$$\vartheta(\xi) = \frac{\left(\frac{t_o}{nt}\sqrt{1-\xi^2}+3\,\frac{(c'_{ap})_o\,t_o}{4\,b}\right)(1-4\,\xi^2)}{1+3\lim_{\xi\to 1}\frac{t_o}{nt}\sqrt{1-\xi^2}+3\,(c'_{ap})_o\,\frac{t_o}{b}}\,\Delta\vartheta \quad .. \tag{16}$$

now from eqs. (11) and (14)

$$c_{a\vartheta}(\xi) = \frac{c'_{ap}\,\vartheta(\xi)}{1+\frac{\vartheta_1(\xi)}{\vartheta_p(\xi)}} = \frac{c'_{ap}\,.\,\vartheta(\xi)}{1+3\,\frac{(c'_{ap})_o\,t_o}{4\,b}\,\frac{nt}{t_o\sqrt{1-\xi^2}}} \tag{17}$$

This formula is very similar in its structure to that of eq. (6a). Substituting (16) into (17), it follows that

$$c_{a\vartheta}(\xi) = \frac{\frac{t_o}{nt}\sqrt{1-\xi^2}(1-4\,\xi^2)}{1+3\lim_{\xi\to 1}\frac{t_o}{nt}\sqrt{1-\xi^2}+3\,(c'_{ap})_o\,\frac{t_o}{b}}\,c'_{ap}\,\Delta\vartheta \tag{18}$$

Again, the lift distribution of a wing of arbitrary plan form should be assessed. Firstly, from the given distribution of the angles of incidence $\alpha(\xi)$ that part $\bar{\alpha}$ must be split off which is due to the plan form. From eq. (16)

$$\bar{\alpha} = \alpha(0)-\vartheta(0) =$$
$$\alpha(0) - \frac{1+3\,\frac{(c'_{ap})_o\,t_o}{4b}}{1+3\lim_{\xi\to 1}\frac{t_o}{nt}\sqrt{1-\xi^2}+3\,(c'_{ap})_o\,\frac{t_o}{b}}\,[\alpha(0)-\alpha(1)] \tag{19}$$

For that part of the lift coefficient which is due to twist, eq. (17) gives a useful value even if the twist $\vartheta(\xi)$ is not of the type presupposed in eq. (14). Discontinuities in lift coefficient due to unsteady twist must be smoothed out.

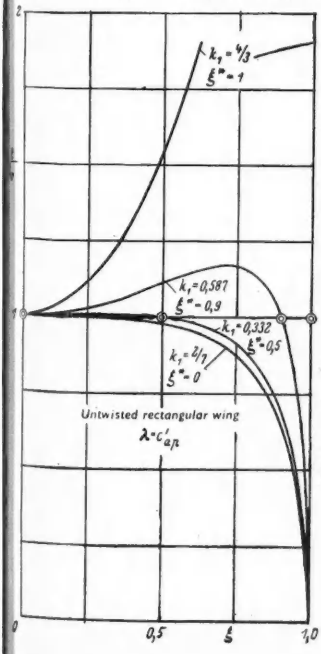



**Fig. 2. Showing how assumptions of example in Fig. 1. are obtained.**

Method: To find $c_a$ at locus $\xi^*$, the given wing contour (here $\frac{t}{t_o}=1$) is replaced by a contour according to eq. (8) with $k_1(\xi^*)$ = const. and $k_2 = 0$. The figure shows four such contours for four various values of $\xi^*$.

**4. The Twisted Elliptic Wing.**

The formulæ become very simple for elliptic plan form of the wing with n = 1. Two types of twist will be considered, the parabolic and the so-called "end twist."

For elliptic contour and parabolic twist eq. (16) becomes

$$\vartheta(\xi) = A\,\vartheta\,(\tfrac{1}{4}-\xi^2) \quad .. \quad .. \quad (20)$$

With $\frac{F}{b\,t_o} = \pi/4$ for the ellipse, and aspect ratio $\lambda = \frac{b^2}{F} = \frac{4b}{\pi t_o}$ one obtains according to eq. (18)

$$c_a\vartheta(\xi) = \frac{(c'_{ap})_o\ A\,\vartheta}{1+3\,\frac{(c'_{ap})_o\ t_o}{4b}}(\tfrac{1}{4}-\xi^2) = \frac{(c'_{ap})_o}{1+3\,\frac{(c'_{ap})_o}{\pi\lambda}}\vartheta(\xi) \quad (21)$$

or $$c_a\vartheta(\xi) = A\,c_a\,(\tfrac{1}{4}-\xi^2) \quad .. \quad .. \quad (21a)$$

With $$A\,c = \frac{c'_{ap}\,A\,\vartheta}{1+3\,\frac{c_{ap}}{\pi\lambda}} \quad .. \quad .. \quad (21b)$$

the "lift drop" $c_c\,(0) - c_a\,(1)$ is introduced which is independent to $\alpha$. The part of lift coefficient due to plan form is constant along the span.

According eqs. (9) and (10), with $k_1 = 0$,

$$c_{a\alpha} = \frac{(c_{cp})_o\ \bar{\alpha}}{1+\frac{(c'_{ap})_o}{\pi\lambda}} = \bar{c} \quad .. \quad .. \quad (22)$$

It is to be stressed that for the parabolically twisted elliptic wing, $c_a\vartheta\,(\xi)$ has itself a parabolic distribution. Further, this type of wing has the peculiarity of being independent of the aspect ratio $\lambda$ at that location of the span for which the zero-lift direction of the local section coincides with the zero-lift direction of the whole wing. This locus is at $\xi = \frac{1}{2}$. A general feature of the twisted elliptic wing is in the possibility of splitting up the induct drag into parts due to plan form and due to twist. As $k_1 = k_2 = \ldots = 0$, and with eq. (4) $c_{co} = \bar{c}_a$, one obtains

$$\bar{c}_{wi} = \frac{\bar{c}_a^2}{\pi\lambda} + \frac{\pi}{16}\,\frac{(c_{aI}\,t_o)^2}{F}\left(1+\frac{k_I}{2}+\frac{k_{II}^2}{4}+\frac{k_{II}}{4}+\right.$$
$$\left.+\frac{11}{32}k_I\,k_{II}+\frac{9}{64}k_{II}^2\right) \quad .. \quad (23)$$

Because of $k_{II} = -8\,(1+\frac{k_I}{4}$ as before,

$$\bar{c}_{wi} = \frac{1}{\pi\lambda}\left[\bar{c}_a^2+c_{aI}^2\left(8+\frac{7}{4}k_I+\frac{k_I^2}{8}\right)\right] =$$
$$= \frac{1}{\pi\lambda}\left[\bar{c}_a^2+c_{aI}^2\left(3-\tfrac{3}{8}\,k_{II}+\frac{k_{II}^2}{32}\right)\right] \quad (23a)$$

further, $$A\,c_a = c_{aI}\,(8+k_I) = c_{aI}\left(4-\frac{k_{II}}{2}\right)$$

and $$A\,\vartheta = \frac{A\,c_a}{c'_{ap}}\left(1+4\,\frac{c'_{ap}}{\pi\lambda}\,\frac{7+k_I}{8+k_I}\right).$$

In the present case $k_{II} = 0$, $k_I = -4$, and in accordance with eqs. (15) and (21) $c_{aI} = \frac{A\,c_a}{4}$.

Thus,

$$\bar{c}_{Wi} = \frac{\bar{c}_a^2}{\pi\lambda} + A\,\bar{c}_{wi} = \frac{\bar{c}_a^2}{\pi\lambda} + \frac{3}{16}\,\frac{(A\,c_a)^2}{\pi\lambda} \quad .. \quad (24)$$

$$c_a\vartheta\,(\xi) = A\,c_a\,(\tfrac{1}{8}-\xi^4) \quad .. \quad .. \quad (25)$$

$$\vartheta(\xi) = \frac{A\,c_a}{c'_{ap}}\left(\frac{1-8\,\xi^4}{8}+2\,\frac{1+6\,\xi^2-20\,\xi^4}{\pi\lambda}\right) \quad (26)$$

$$A\,c_a = \frac{c_{ap}\,A\,\vartheta}{1+\frac{7}{2}\,\frac{c'_{ap}}{\pi\lambda}} \quad .. \quad .. \quad (27)$$

$$\bar{c}_{Wi} = \frac{\bar{c}_a^2}{\pi\lambda} + A\,\bar{c}_{wi} = \frac{\bar{c}_a^2}{\pi\lambda} + \frac{1}{8}\,\frac{(A\,c_a)^2}{\pi\lambda} \quad .. \quad (28)$$

Another type of twist which will be analysed is the "end twist." This is characterized by the disappearance of all coefficients except $k_{II}$ in the series eq. (11). Here, the details of the calculation are omitted, and the results will be given only:

The comparison of eq. (24) and eq. (28) shows that the elliptic wing with end-twist has the smaller induced drag than the same wing with parabolic twist. This is brought out even more clearly by a comparison of eq. (21b) with eq. (27) which shows also that with $c_a\vartheta$ and $\vartheta$ are not distributed in the same way.

# COMPUTATION OF STEAM BOILERS BY A GRAPHICAL METHOD.

By Dr.-Ing. TH. GEISSLER, Mor. Ostrava. (From *Die Wärme*, Vol. 66, No. 17, August, 1943, pp. 185-188).

THE graphical method of steam boiler computation is based on the i - t diagram (Fig. 1), giving the heat content i per Nm³ flue gas (m³ at 0 °C, 760 mm. Hg.) for various excess air percentages at any given temperature $t_g$ covered by the diagram. Charts of this kind, based upon the analysis of the fuel in question, naturally are superior in accuracy to such solely based on the heating value of the fuel. However, even graphs of the latter type possess an accuracy exceeding that of the measuring instruments used in present-day boiler practice.

If $L_o$ is the theoretical percentage of combustion air, $V_o$ is the theoretical flue gas volume, and n is the excess air factor, the percentage of air contained in a flue gas is given by:

$$v = \frac{(n-1)\,L_o}{V_o + (n-1)\,L_o}\,.\,100\,(\% \quad .. \quad .. \quad (1$$

The specific flue gas volume $V_g$ for the prevailing excess air factor n and the theoretical air requirement $L_o$ can be obtained from the empirical formulae established by Fehling and Rosin, to wit:—

$$L_o = \frac{1.01\cdot H_u}{1{,}000} + 0.5\ (Nm^3/kg) \quad .. \quad .. \quad (2a)$$

$$V_o = \frac{0.89\cdot H_u}{1{,}000} + 1.65\ (Nm^3/kg) \quad .. \quad .. \quad (2b)$$

$$V_g = V_o + (n-1)\cdot L_o \quad .. \quad .. \quad (2c)$$

The boiler computation proper may be made on the basis of 1 kg of steam to be generated. The excess air factor to be employed, is usually chosen on the basis of practical experiences; it is related to the $CO_2$ content of the flue gas by the expression $n = K_{max}/K$, where $K_{max}$ is the highest theoretically possible $CO_2$ content with a given fuel, while K represents the actual $CO_2$ content of the flue gas under operating conditions.

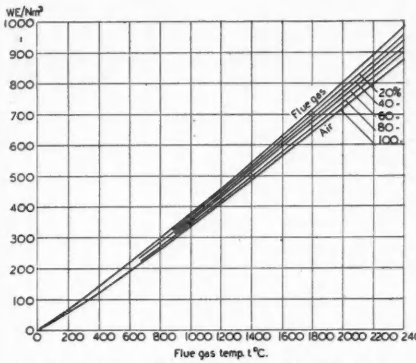



Fig. 1. **i-t diagram of flue gas for different flue gas temperatures and excess air factors.**

## Determination of the theoretical combustion temperature in the furnace.

The sensible heat contained in one Nm³ of flue gas during combustion in the furnace is given by

$$i_f = \frac{H_u + Q}{V_g} \quad .. \quad .. \quad .. \quad .. \quad (4)$$

where $H_u$ is the lower calorific value of the fuel, and Q is the heat imparted to the combustion air in the air preheater. The fact that only a portion of the combustion air is passed through the air preheater—the balance entering the furnace as false air—, is taken into account by introducing the factor $\epsilon$ ranging from 0.8 to 0.9. It is $Q = \epsilon \cdot n \cdot L_o\,(i_a - i_e) + n \cdot L_o \cdot i_e$, where $i_a$ is the heat content of the preheated air, and $i_e$ is that of the cold air. It follows:

$$i_f = \frac{H_u + \epsilon \cdot n \cdot L_o\,(i_a - i_e) + n \cdot L_o \cdot i_e}{V_g} \text{ (Kcal/Nm}^3\text{)} \quad (5)$$

In this equation, $n \cdot L_o \cdot i_e$ is small enough to be neglected and it therefore is:

$$i_f = \frac{H_u + \epsilon \cdot n \cdot L_o\,(i_a - i_e)}{V_g} \text{ (Kcal/Nm}^3\text{)} \quad .. \quad (5a)$$

By entering this value into the i—t diagram (Fig. 2), the theoretical combustion temperature $t_{th}$ is obtained. With

$$i_1 = \frac{H_u}{V_g}, \text{ the difference } i_f - i_1 = \Delta i_1,$$

which indicates the increase in sensible heat caused by the air, can be read off.

## Stack temperature.

The lowest datum line in Fig. 2 represents the stack temperature (generally taken as 180-200 deg. C.). By plotting $\Delta i_1 = i_f - i_1$ over $i_g$, the temperature at the economiser outlet is found (Fig. 2), thus limiting the computation of temperature distribution within the boiler to the zone bordered by the lines $i_f$ and $i_2$. It is the difference of these two heat contents which serves to evaporate the steam from feedwater at $t_p$ deg. C. and to subsequently superheat it to the final temperature tü.

## The total heat q required for steam generation.

It is $$q = \Delta i_{ü} + r' + \Delta i_{sp} \quad .. \quad .. \quad (7)$$

$$\Delta i_{ü} = i_{ü} - i'' + \frac{x \cdot r}{100} \quad .. \quad .. \quad .. \quad 8)$$

$$r' = r\,(1 - x),$$

where $i_{ü}$ = heat content of the superheated steam
$i''$ = heat content of the saturated steam
r = heat of evaporation
x = percentage of water evaporated in the superheater and of water injected for desuperheating

$$\Delta i_{sp} = \left(1 + \frac{y}{100}\right)(i' - i_{sp}) \quad .. \quad .. \quad (9)$$

$i'$ = heat content of the water at saturation temperature
$i_{sp}$ = heat content of the water entering the economiser
y = percentage of water to be fed in excess in order to compensate for blow-off loss.

$$q = \left(i_{ü} - i'' + \frac{x}{100} \cdot r\right) + r' + \left(1 + \frac{y}{100}\right)(i' - i_{sp}) \quad (10)$$

An increase in the heat content of the water or of the steam by the amount $\Delta i_w$ corresponds to a decrease in the heat content of the flue gas by the amount $\Delta i_g$. It will, therefore be on the basis of 1 kg of steam:

$$B \cdot V_g \cdot \Delta i_g = \Delta i_w \quad .. \quad .. \quad .. \quad .. \quad (11)$$

$$B = \frac{q \cdot \eta_f}{H_u \cdot \eta_k}$$

$$\Delta i_w = \frac{V_g \cdot q \cdot \eta_f}{H_u \cdot \eta_k} \Delta i_g \quad .. \quad .. \quad .. \quad (12)$$

where B = the amount of fuel actually burnt
$\Delta i_g$ = decrease in flue gas heat contents
$\Delta i_w$ = increase in the heat content of water or steam
$\eta_k$ = boiler efficiency
$\eta_f$ = firing efficiency

## Determination of the flue gas temperatures before and after the heating surfaces.

Equation (12) can be easily solved graphically if the radiation loss of the boiler and the temperature drop

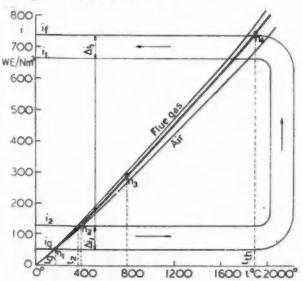



Fig. 2. **i-t showing variation of excess air factor through boiler.**

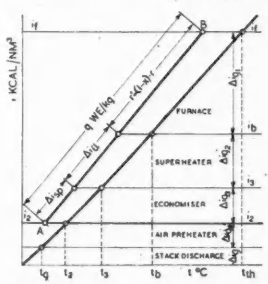



Fig. 3. **Determination of flue gas temperature in heating surfaces of radiant boiler.**

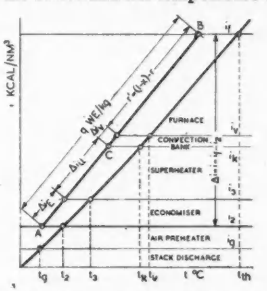



Fig. 4. **Determination of flue gas temperatures in radiant boiler with convection bank before superheater.**

of the flue gases caused by the entering of false air are assumed to be proportional with the quantities of heat absorbed by the individual heating surfaces. This graphic solution is carried out by plotting the value of q on a somewhat larger scale than that used for the i-values in the i-t diagram (Fig. 3). Starting from an arbitrarily chosen point A on the $i_2$ - horizontal, the line A-B = q is easily located by describing a circle of the radius q with point A as centre and finding point B on the $i_f$ line as point of intersection between that line and the circle. With this, equation (12) is solved, and at the same time the heat content i is now obtained in units of Kcal/kg instead of Kcal/Nm³. By subdividing q into the fractions $\Delta i_{sp}$, $\Delta i_{ü}$, and r′, and plotting these values in proper sequence along A-B, the corresponding heat contents can be found by drawing the horizontal lines $i_b$ and $i_3$, as shown in Fig. 3. It is also seen that the points of intersection of these lines with the i-t curve readily yield the corresponding flue gas temperatures $t_b$ and $t_3$.

This graph can thus be used to ascertain the gas temperature $t_b$ required at the furnace outlet if the entire heat of evaporation is to be absorbed in the furnace, as is the case in radiant boilers. If a steaming economiser is to be provided to evaporate, e.g., 5 per cent of the feedwater, the furnace outlet temperature will be found by decreasing the r′ value on the B-A line to 95 per cent. of its original value, drawing a new $i_b$ line.

**Radiant Boiler with convection surface and steaming economiser.**

In most cases the furnace outlet temperature will be in excess of that permissible at the superheater inlet. If, for instance, choking of the superheater surface by slag deposits is to be feared, a diminution in flue gas temperature will have to be effected by providing boiler surface between superheater and furnace. In such cases, the gas temperature $t_k$ at the superheater inlet—chosen below the softening temperature of the slag—is entered in the i-t diagram as shown in Fig. 4. The horizontal line drawn through the point of intersection C will then divide the distance q into two parts A-C and C-B. By plotting r′ along B-C, the difference $\Delta i_v$ is obtained which represents the amount of heat to be absorbed in the boiler convection surface per kg of steam. By plotting $\Delta i_ü$ from point C on downward along the line A-C, the heat $\Delta i_e = \Delta i_{sp} - \Delta i_v$ available for feedwater heating in the economiser is obtained.

However, if the combustion chamber characteristics have already been decided upon, the furnace outlet temperature $t_b$ can be established by any of the usual methods. This will then yield the point C on the line A-B (Fig. 5). By plotting r′ from point B, it can then be ascertained whether the furnace will be capable of absorbing all the heat required for evaporation. Should it be found that B-C is smaller than r′ (or smaller than 0.95 r′ in the case of a steaming economiser), a convection boiler surface will be required. But should B-C be found larger than or equal to r′ (or 0.95 r′), the superheater can receive the gases direct from the furnace.

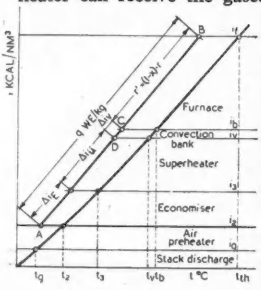



Fig. 5. **Determination of flue gas temperatures in case of pre-determined furnace characteristics.**

In such a case a convection surface between furnace outlet and superheater will be required only if the furnace outlet temperature should be adjudged unduly high from the view point of superheater operation. By plotting the distance $\Delta i_ü$ from D downward, the size

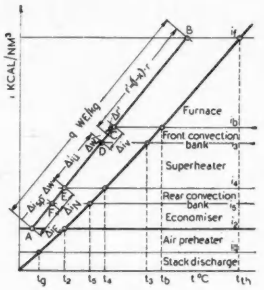



Fig 6.

**Flue gas temperature distribution in boiler with boiler convection surfaces before and after superheater.**

of the economiser is found from $\Delta i_E$, at the same time obtaining the temperature $t_3$ of the flue gases leaving the superheater.

**Water tube boiler with boiler convection surface before and after the superheater.**

The graphical method is particularly useful for determining the distribution of flue gas temperatures and boiler surfaces in the case of bent-tube boilers. As in the design of boilers of this type the furnace is nearly always laid out first, the furnace outlet temperature $t_b$ will have to be determined first with the use of one of the usual methods. By entering this temperature in the i-t diagram (Fig. 6) the point of intersection C is obtained. However, with the furnace layout, the shape of the boiler bank between furnace and superheater is also given, and it is thus possible to determine its size and its heat absorption $\Delta_v$. By plotting this heat absorption and also the heat absorption $\Delta i_ü$ of the superheater along line A-B from point C on downward, the respective points D and E on this line can be found. Starting from point B, the distance corresponding to r′ is now also plotted along line B-A.

In most cases it will be found that the furnace chamber is incapable of evaporating the entire amount of water corresponding to the specified boiler load, and the end point of the distance r′ will therefore be located somewhere in the convection surface $\Delta i_v$. If the amount of water evaporated in the front bank of the boiler is expressed by the amount of heat $\Delta r'$, the heat absorption in this boiler surface will be utilised also for heating a certain amount of water to saturation temperature imparting to it the amount of heat $\Delta w$. The amount of heat $\Delta i_{sp} - \Delta w$ will therefore remain available for preheating the water in the second boiler bank and in the economiser. The amount of heat $\Delta i_N$ absorbed in the second boiler bank can be found by computing the area and heat transfer in that surface. The numerical value of the heat absorption will then serve to establish the actual length of the distance E-F in the graph. The remaining distance A-F will then represent the amount of heat $\Delta i_E$ left for absorption in the economiser. Vice versa, by deciding first on the gas temperatures required to prevail at the various points of the boiler, the corresponding heat contents to be absorbed in the respective heating surfaces can be read off. Computation of the required surface areas is then carried out in the usual way.

In order to safeguard water circulation in the boiler circuit, it is generally required that steaming should take place solely in the front bank of the boiler and not in the rear bank. This condition will be met if the endpoint of r′, extending from B, happens to be located in the surface $\Delta i_v$ (Fig. 6). But should it be located in $\Delta i_ü$, this would serve to indicate that the joint heat absorption of furnace and boiler front bank is smaller than the heat required for complete evaporation of the water, so that a certain amount of evaporation will have to take place in the rear bank of the boiler.

# ELECTRIC PURITY CONTROL OF BOILER FEED-WATER

By ING. DR. V. HOFFMANN. (From *Elektrotechnicky Obzor*, Prague, Vol. 31, No. 12, December, 1942, pp. 185-185.

In steam power stations, particularly in those operating at highest steam pressures, special care must be taken to ensure the highest purity of the raised steam and, consequently, that of the feed water. Salts contained in the steam settle on the turbine blading which, after a few days or after a few hundred hours of operation, may be covered by a thick layer of various deposits, usually consisting of Na Cl, Na OH, $Na_2CO_3$ and $Na\ SO_4$.

One of the most sensitive methods of measuring the salt contents of water consists in measuring its electric conductivity. This method can, however, be used only to detect substances dissolved or dissociated in the water and these are actually the only ones of practical importance.

The specific conductivity of various salts differs and is largely dependent on the temperature of the solution. The practical example given in the following, refers to a solution of K Cl, which was used also for calibrating the measuring apparatus.

Fig. 1 shows the specific resistance of a solution of K Cl as a function of the concentration of the solution at various temperatures. (The specific resistance is the resistance of a column of the liquid, of 1 cm length and 1 $cm^2$ cross sectional area). Both scales of the graph are logarithmic in which system the function appears as a straight line. Actual points measured are marked on the line for 17.5°C. and only the last point diverges. But it must be born in mind that the solution was not diluted by water of infinite ohmic resistance (absolutely pure water), and this fact certainly influenced the last measurement which referred to an extremely diluted solution.

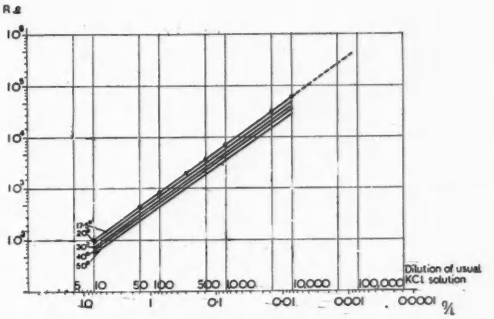



Fig. 1 Specific resistance of a KCl solution as function of its degree of dilution.

Fig. 1 also shows that with increasing temperature the specific resistance decreases. This decrease is rapid at first and becomes slower by degrees. The angle of the various lines in Fig. 1 is also different, which all indicates the importance of temperature in the measurement.

The relation between the specific resistance and the concentration of the solution K Cl is best represented mathematically by the following general equation:

$$R = m \cdot X^n,$$

(R = specific resistance, X = content of salt in the solution, m and n are functions of the temperature of the solution).

The resistance can be measured by any bridge method. In particular the small portable "Philiskop" made by Philips proved very useful. In principle, this instrument consists of a Wheatstone bridge for a.c. where the normal zero indicator has been replaced by a cathode-ray indicator. The measurement is carried out with current at 50 cycle frequency or, for more concentrated solutions, with currents at higher frequencies.

The control of the feed-water, especially on power stations where the make-up water is distilled before

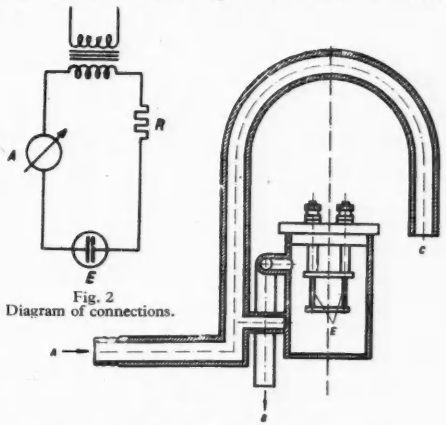



Fig. 2
Diagram of connections.

Fig. 3. Water container with measuring electrodes
*A* Inlet, *B* Outlet, *C* Overflow, *E* Electrodes

(*Continued from previous page*)

dividing head worm wheel. This most important test indicates the precision working possible with the machine and the accuracy of the parts which have been cut on the machine. (Fig. 4).

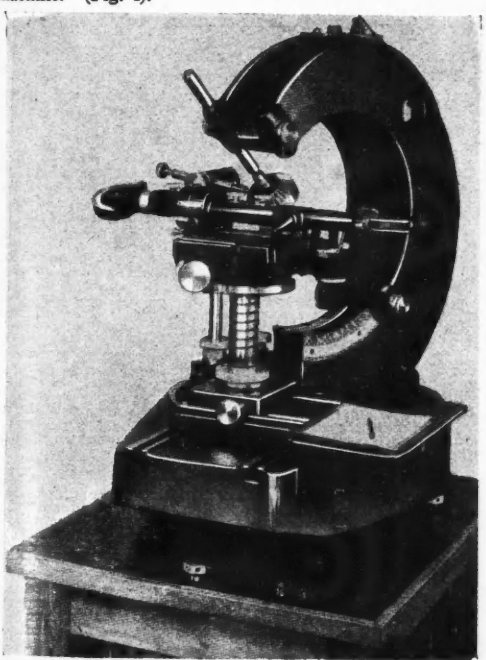


Fig. 4

being added to the water circuit, should be preferably continuous and be recorded automatically on a suitable instrument (Fig. 5). A schematic diagram of the apparatus used is shown in Fig. 2, where A is a recording milli-ammeter, E the measuring electrode, and R a protective resistance. The frequency of the current used was 50 cycles.

The feed-water temperature was so high that complications in the measurement became inevitable and small quantities of water were taken by a sampling apparatus from the pipes for testing. After passing through a cooling system where it is cooled to 14°—25° C., the water was admitted to the measuring electrodes.

Fig. 3 shows the water container with the measuring electrodes. A Water inlet, B Water outlet, C Overflow, and E Measuring electrodes. The overflow tube ensures the water in the container being calm as the flow through the cooling system is not always smooth. The container is of nickel plated metal, the electrodes of stainless steel and the insulation is of ebonite. Fig. 4 shows a photograph of the set.

The same apparatus is used for measuring the purity of steam. In this case the steam is condensed and the purity of the condensate is measured.

No special precautions are necessary to maintain an exactly constant water temperature, as the temperature of the condensate of a power station remains largely constant and, as this apparatus does not serve for absolute measurements but only for comparative measurements, its main purpose is to indicate exceptional deviations from the normal values. The exact and absolute daily measurements of the purity of steam and feed-water must, of course, be carried out in the laboratory.

Fig. 5 shows a recording made by the apparatus described. The scale directly indicates the specific resistance. A sudden decrease of purity will be noticed; this was caused by a fault in the automatic distillation apparatus in the station.

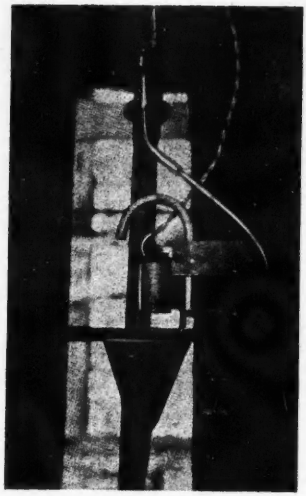


Fig. 4

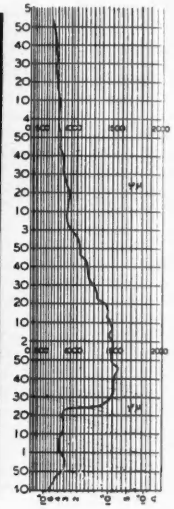


Fig. 5

The measuring set described is very simple and has proved extremely useful. The purity of the feed-water is controlled continuously and any disturbance due to faults or faulty manipulation is immediately registered

Practical results show that after the device had been in operation for six months, the quantity of water lost through boiler cleaning was 2437 tons below the corresponding figure for the previous year, which means a saving of $600 \times 10^6$ kgcal or approximately 175 tons of coal.

# IRON-SILICON ALLOY OF HIGH INITIAL PERMEABILITY

(From *V.D.I. Zeitschrift*, Vol. 87, No. 35-36, September 4th, 1943, pp. 572-573).

WHEREVER high magnetization must be achieved with small field strength, iron-nickel alloys with about 40 per cent nickel are almost exclusively employed. To the substitution of this material by iron-silicon alloys, the uneveness of the magnetic properties of these alloys remains an obstacle even in cases where the greater amount of material and the larger number of coil windings can be considered acceptable. Theoretically, an initial permeability of 16000 in the case of pure iron and of 6000 in the case of iron-silicon alloys should prevail; but actually less than one-tenth of these values is achieved.

Attempts to increase the initial permeability have been undertaken in two different directions. While T. D. Yensen and his co-workers achieved considerable improvements by careful selection of the raw materials and by carrying out the melting in the high frequency furnace under high vacuum, P. P. Cioffi used commercial alloys and applied hydrogen under pressure shortly below the melting point. With this method, Cioffii was able to obtain permeabilities closely approaching the theoretical value. As neither of these methods is applicable to commercial production, an attempt was made to devise a process with the use of commercial iron-silicon alloys and the employment of the usual industrial heat treatment.*

The material used was cold rolled strip with about 3 per cent Si, of a relatively pure material. In a preliminary test, a number of stampings were heated at 1350 deg. C. (a) in commercial hydrogen, (b) in hydrogen of high purity electrolytically produced in the laboratory, (c) in a mixture of 25 per cent $N_2$ and 75 per cent $H_2$ (dissociated ammonia), and (d) under a vacuum produced by a three stage diffusion-type mercury pump. With the electrolytically produced hydrogen and with the vacuum process, initial permeabilities of about 1800 were obtained; while the 25 per cent $N_2$/75 per cent $H_2$ and the commercial hydrogen treatments yielded no more than initial permeabilities of about 300. An investigation of the influence of the process temperature carried out with the high-purity hydrogen showed that at least 1300 deg. C. are required to achieve really satisfactory results.

Since it had to be assumed that water vapour, nitrogen, or carbon had been responsible for the lack of success with the Nitrogen-Hydrogen mixture and with the commercial hydrogen, a special investigation of these media upon the initial permeability was made. This showed water vapour to be harmless, while minute percentages of nitrogen or carbon (the latter being added to the hydrogen in the form of naphtalene vapour) sufficed to cause a considerable decrease in initial permeability, 0.025 per cent by volume $N_2$ decreasing the latter by more than one-half; and a nitrogen content of 0.1 per cent by volume sufficed to decrease the permeability to that of commercial materials. The influence of the carbon proved still greater, an addition of $4.10^{-5}$ per cent by volume resulted in a decrease in initial permeability by one-half; while $3.10^{-4}$ per cent depressed it to the commercial level. It is remarkable that all test pieces exhibited identical coercive forces and maximum permeability. Deterioration of the latter occurred only with a field strength of approximately 0.06 Oersted and less, that is, in the range particularly important in telecommunication apparatus.

The action of the pure hydrogen is explained by the fact that its application leads to the removal of oxides, and of phosphorus, sulphur, and carbon compounds from the piece, owing to the formation of the corresponding hydrogen compounds. In the case of the vacuum treatment, the purifying action of hydrogen is due to the chemical combination ensuing between the oxides with the carbon, and to the removal of the gaseous products formed at the heating temperature. Here again, nitrogen constitutes the component most difficult to remove, thus necessitating the application of especially high vacua. By extending the investigation to core sheets and cable armouring silicon irons, it was found that both the pure hydrogen and the vacuum treatment lead to a considerable improvement of these materials. With this, the substitution of 40 per cent nickel iron by a nickel free material has now become a reality.

*F. PAWLEK. *Arch. Eisenhüttenw.*, Vol. 16 (1942-43), p. 363.

# TECHNICAL NEWS

*Announcements in this section include News relating to British Firms, and on Equipment produced by British Manufacturers.*

*Available literature may be secured by addressing a request to the advertising department of "The Engineers' Digest," or by writing direct to the manufacturer and mentioning "The Engineers' Digest" as a source.*

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- ✦ BUSINESS CHANGES
- ✦ PERSONAL
- ✦ NOTES

## ENGINEERS IN THE NEW YEAR HONOURS LIST.

**KNIGHTHOODS HAVE BEEN CONFERRED UPON :—**

**Mr. G. E. Bailey,** C.B.E., M.Sc., M.I.Mech.E., M.I.E.E., Works director, Metropolitan-Vickers Electrical Co. Ltd.

**Mr. C. J. Bartlett,** M.I.A.E., Managing director, Vauxhall Motors Ltd.

**Capt. Geoffrey de Havilland,** C.B.E., A.F.C., F.R.Ae.S., director, De Havilland Aircraft Co. Ltd.

**Dr. J. J. Fox,** C.B., O.B.E., F.I.C., Government chemist.

**Mr. W. T. Halcrow,** M.Inst.C.E., M.I.Mech.E., engineering consultant, War Office.

**Dr. T. R. Merton,** M.A., F.R.S., Scientific adviser, Ministry of Production.

**Mr. J. G. Nicholson,** deputy chairman, Imperial Chemical Industries Ltd.

**Mr. W. Paterson,** M.I.Mech.E., chairman, Paterson Engineering Co. Ltd.

**Dr. A. H. Railing,** M.I.E.E., chairman and general manager, General Electric Company Ltd.

**Mr. F. C. Stewart,** chairman, Thermotank Ltd.

**Mr. C. S. Swan,** M.I.N.A., chairman, Swan, Hunter and Wigham Richardson, Ltd.

**K.B.E. :—**

**Mr. C. J. Radcliffe,** K.C., director general, Ministry of Information.

**C.O.B. :—**

**Mr. C. S Lillicrap,** M.B.E., R.C.N.C., M.I.N.A.

**Major-General E. B. Rowcroft,** in command of the R.E.M.E.

**C.B.E. :—**

**Mr. W. A. Akers,** director of Research, Department of Scientific and Industrial Research.

**Mr. W. F. Bishop,** director, W. T. Henley's Telegraph Works Ltd.

**Mr. W. T. Butterwick,** M.I.N.A., deputy director, Merchant Shipbuilding, Admiralty.

**Mr. W. H. Glanville,** director of Research, Department of Scientific and Industrial Research.

**Mr. W. W. Hackett,** managing director, Accles and Pollock, Ltd.

**Mr. N. E. Rowe,** director, Technical Development, Ministry of Aircraft Production.

**Mr. H. C. Whitehead,** M.Inst.C.E., Chief Engineer, Birmingham.

**Mr. H. M. Woodhams,** director and general manager, Sir W. G. Armstrong-Whitworth Aircraft Ltd.

**COMPANION OF THE ORDER OF THE INDIAN EMPIRE :—**

**Mr. A. MacL. Robertson,** chief mechanical engineer, Bengal Nagpur Railway.

**O.B.E. :—**

**Mr. I. Anderson,** M.I.N.A., chief technical officer, Anti-Submarine Experimental Establishment, Admiralty.

**Mr. D. Bellamy,** general manager, Hull Corporation Electricity Department.

**Mr. H. R. Humphreys,** M.I.N.A., director, Cammell Laird & Co. Ltd.

**Dr. J. G. King,** A.R.T.C., F.I.C., director, Gas Research Board.

**Mr. E. J. Lowe,** works manager, Thos. Firth & John Brown Ltd.

**Mr. W. L. Nelson,** M.I.Mar.E., chief superintendent, Eagle Oil and Shipping Co. Ltd.

**Mr. L. H. Ripley,** Principal Surveyor for Steel, Lloyd's Register of Shipping.

**Mr. D. H. Thomson,** M.A., M.Inst.C.E., M.I.Mech.E., engineer of the Portsmouth Water Company.

**Mr. W. Wallace,** managing director, Brown Brothers Ltd.

**M.B.E. :—**

**Mr. H. S. Glass,** chief engineering draughtsman, W. H. Allen, Sons & Co. Ltd.

**Mr. H. H. Holloway,** chief engineer, Birmingham Carriage and Wagon Co. Ltd.

**Mr. W. Hoy,** manager, North Eastern Marine Engineering Co. (1938) Ltd.

**Mr. J. B. Kerr,** chief draughtsman, Wallsend Slipway and Engineering Co. Ltd.

**Mr. D. McNicoll,** works manager, British Timken Ltd.

**Dr. H. E. Merritt,** M.I.Mech.E., M.I.A.E., technical manager, David Brown Tractors Ltd.

**Mr. T. R. Porter,** production engineer, Metropolitan-Vickers Electrical Co. Ltd.

**Mr. I. S. Sinclair,** assistant works manager, Alfred Herbert Ltd.

**Mr. I. Strachan,** production manager, Consolidated Pneumatic Tool Co. Ltd.

### PERSONAL.

**Mr. D. H. Alexander,** M.Sc., Principal of the Belfast College of Technology, has been elected President of the Belfast Association of Engineers.

**Mr. E. H. Armitage** has been appointed director of Murex Ltd.

**Alderman C. S. Bache,** J.P., has been appointed chairman and managing director of Messrs. Geo. Salter & Co. Ltd., West Bromwich.

**Commander Sir Charles Craven,** Bt., R.N., has been appointed deputy chairman of Vickers Ltd., to succeed Col. J. B. Neilson, D.S.O. Col. Neilson remains on the board of the Company.

**EDITORIAL INDEX.** The Editorial Index to Vol. IV., January-December, 1943, will be sent to all Subscribers of our journal free on application.


# HEAT TREATMENT SPECIALISTS

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**Mr. E. W. Connon,** B.Sc., M.Eng., A.M.I.E.E., has been appointed chief technical assistant, Sheffield Corporation Electric Supply.

**Mr. James Fielding,** managing director of Messrs. Fielding & Platt Ltd., and **Mr. W. M. Good,** have been appointed to the Board of Messrs. Heenan and Froude Ltd.

**Mr. I. Frost,** A.M.I.Mar.E., has been appointed general manager to Messrs. F. Bamford & Co. Ltd., Waterloo Road, Stockport.

**Mr. S. E. Goodall,** M.Sc., M.I.E.E., has been appointed assistant chief engineer to Messrs. W. T. Henley's Telegraph Works Co. Ltd.

**Mr. Albert Gordon** has been appointed director of Parkinson & Cowan Ltd., the Gas apparatus manufacturers.

**Mr. Alfred R. Habershon,** managing director of Messrs. J. J. Habershon & Sons Ltd., Holmes Mills, Rotherham, died recently at the age of 62.

**Mr. S. C. Harling,** A.M.I.E.E., A.I.Mech.E., has been appointed borough electrical engineer, Leigh Corporation Electric Supply, Lancashire.

**Mr. K. Headlam-Morley,** Secretary of the Iron and Steel Institute, has accepted an appointment in a consultative capacity with the Iron and Steel Control.

**Mr. I. L. Hilton,** A.M.I.Mech.E., has been appointed chief engineer to the Hoffmann Manufacturing Co. Ltd.

**Mr. Norman Iles** has been appointed director of plant progress in the Iron and Steel Control.

**Sir Francis Joseph,** Bt., K.B.E., D.L., M.I.Min.E., has been elected President of the Institute of Industrial Administration.

**Mr. A. M. Maddox, Mr. J. K. Vivian, and Mr. B. A. Williams** have been appointed directors of Williams & Williams Ltd., metal window manufacturers.

**Mr. W. Melland** has been elected President, and **Sir Lawrence Chubb,** honorary treasurer of the National Smoke Abatement Society.

**Mr. H. C. F. Mockridge** has been elected director of the International Nickel Company of Canada Ltd.

**Mr. I. Y. Moyes,** Chairman of Messrs. Mechans, Ltd. Structural engineers and Founders, Scotstown Ironworks, Glasgow, Honorary Secretary of the Scientific Society of the Royal Technical College, died recently at the age of 66.

**Mr. W. M. Ratcliffe,** M.I.Mech.E., managing director of Messrs. Heenan and Froude Ltd., has been elected chairman of Court Works Ltd.

**Sir Louis F. Pearson,** C.B.E., Chairman of the Beeston Boiler Co. Ltd., Nottingham, has died recently.

**Mr. I. R. Petrie,** B.Sc. (Eng.), M.I.Mech.E., has been appointed Principal of St. Helens Municipal Technical College.

**Mr. P. C. Sharp and Mr. A. P. Quarrel,** A.M.I.Mech. E., A.M.I.Mar.E., have been appointed directors of Messrs. Petters Ltd.

**Mr. Norman Spurgeon, Mr. A. C. Burdon and Mr. E. W. Swales,** have been appointed directors to the Lockhead Hydraulic Brake Co. Ltd.

**Mr. Cyril Tasker,** a native of Manchester, who was, until 1930, on the Staff of the British Fuel Research Board, has been appointed Director of Research of the American Society of Heating and Ventilating Engineers.

**Mr. John Wadell,** J.P., has been elected chairman of the Projectile and Engineering Co. Ltd.

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**Conference on X-Ray Analysis.** A third conference on "X-Ray Analysis in Industry" has been arranged to take place in Oxford on March 31 and April 1 under the auspices of the Institute of Physics. Further particulars may be obtained from Dr. H. Lipson, c/o Crystallographic Laboratory, Free School Lane, Cambridge.

**B.S. 1138, 1943. TEST PIECES FOR PRODUCTION CONTROL OF ALUMINIUM ALLOY SPOT WELDS.**

THE above British Standard deals with test pieces for shear testing of spot welds of aluminium base alloys and is intended to standardise production control procedure and to provide reliable indications of the quality of welds.

The Standard describes two test pieces, one of which is for material up to 18 S.W.G. (0.048 in.) thick and the other for thicknesses exceeding 18 S.W.G. In addition, there are clauses dealing with the welding procedure, the testing procedure and the various test results which should be recorded. Price 1/-.

**B.S. 1126. GAS WELDING OF ALUMINIUM AND CERTAIN ALUMINIUM ALLOYS.**

THE Specification gives details of the quality of material which can be gas welded by reference to the appropriate British Standards for various aluminium alloys, the majority of which are in the Aircraft Series. They cover material in the form of sheet castings, tubes, bars and sections. An appendix is included which gives the details of chemical composition and mechanical properties of all of the alloys quoted. Other clauses cover such requirements as the quality of filler rod, the flux (a suggested composition is also given), type of joint, mechanical tests and inspection. Illustrations of recommended forms of joints are also shown. Price 2/-.

**P.D. 191. NICKEL SCHEDULE.**

THE B.S.I. has just issued an additional section covering nickel and nickel alloys, for inclusion in B.S./STA. 7. Services Schedule of Non-ferrous Metals. The new section to the schedule includes tables giving the mechanical properties and tables giving the relevant uses and general notes for all metals covered. The tables have been issued in loose leaf form so that they can be inserted in cover of B.S./STA. 7 in their appropriate place.

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